The WELDING JOURNAL

MAY 1954

IN THIS ISSUE

Stainless Steel Welding Filler Metal 433

Shielded-Metal-Arc Cutting and Grooving 438

Government Standards 446

Tolerance and Control on Fillet Welds 449

Austenitic Manganese Steel Welding Electrodes 459

Directionality of Properties in Hot-Worked Steels 209-s

Working Conditions Ambient to Inert Arcs 218-s

High-Pressure Vessel Tests 230-s

Stresses for Pressure Vessels and Boilers 239-s

Survey of High-Strength Steels 251-s

Table of Contents 431

Index to Advertisers 524

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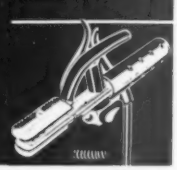

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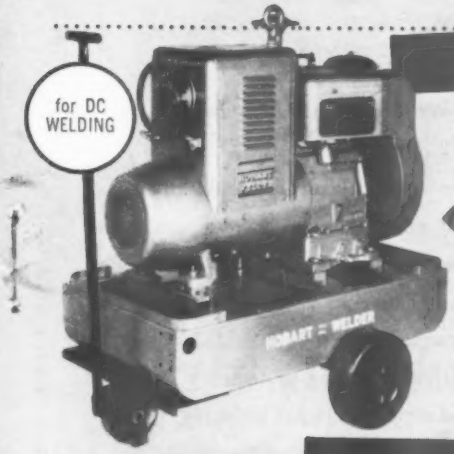

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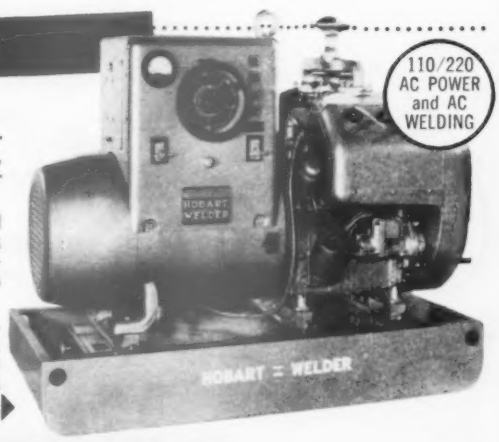

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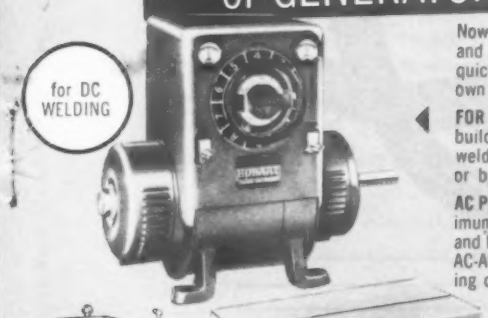

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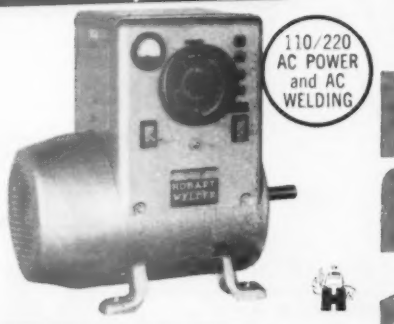

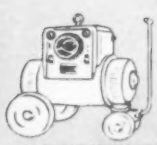

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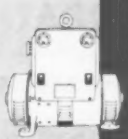

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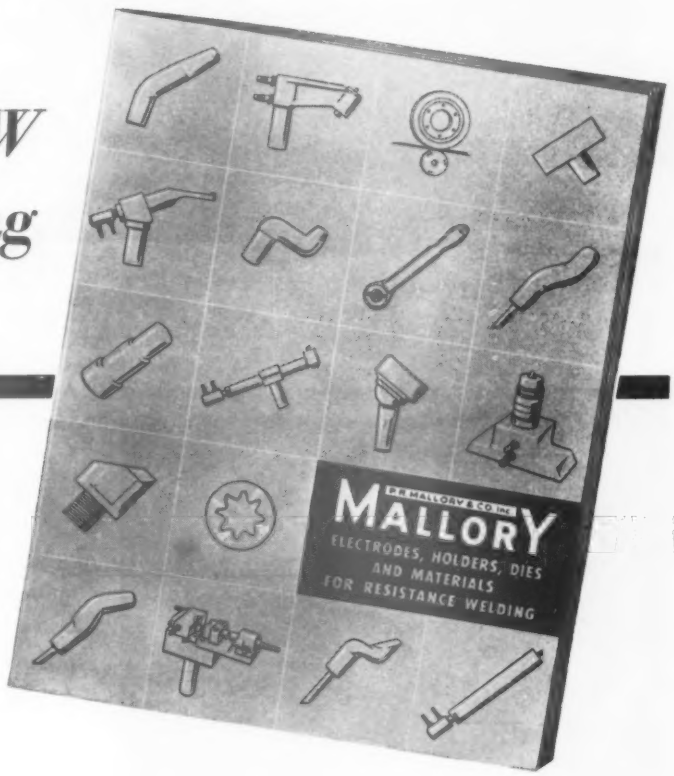

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TECHNICAL PAPERS, ITEMS AND REPORTS

Stainless Steel Welding Rods and Bare Electrodes 433
Shielded-Metal-Arc Cutting and Grooving, by Helmut Thielsch and Joseph Quaas 438
Government Standards, by S. P. Kaidanovsky 446
Current Welding Literature 448
Tolerance and Control on Fillet Welds, by John Mikulak and Merle Dillman 449
Austenitic Manganese Steel Welding Electrodes, by Howard S. Avery and Henry J. Chapin 459
Discussion by T. E. Norman 479

PRACTICAL WELDER AND DESIGNER

Diesel Engine Valves Reclaimed by Sprayweld Process, by L. V. La Rou 480
Kelcaloy Transition Sections 481
Flash Welding Tubing to Forgings 482

SOCIETY AND RELATED EVENTS

Society News 483
News of the Industry 486
New Products 490
Personnel 502
Employment Service Bulletin 502
New Literature 504
Abstracts of Current Welding Patents 510
List of New Members 514
Section News and Events 516
Index to Advertisers 524

WELDING RESEARCH SUPPLEMENT

Directionality of Mechanical Properties in Hot-Worked Steels, by A. R. Troiano and L. J. Klingler 209-s
Welding Metallurgy of Nodular Cast Iron, Discussion by T. E. Kihlgren 217-s
Working Conditions Ambient to Inert-Gas-Shielded Metal-Arc Welding, by Leslie Silverman and Harry Gilbert 218-s
High-Pressure Vessels Subjected to Static and Dynamic Loads, by E. D. Narduzzi and Georges Welter 230-s
Stresses for Pressure Vessels and Boilers Up to 650° F, by W. P. Kerkhof 239-s
Literature Survey of High-Strength Steels 251-s

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The WELDING JOURNAL

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Stainless Steel Welding Rods and Bare Electrodes

Two years in the drafting, the new specifications for bare wire filler metals are now available as AWS Specification A5.9-53T or ASTM Specification A371-53T. Prior to its issuance, the Subcommittee prepared the following explanation of the composition requirements in these specifications

DESPITE the great many advances which have been made in the science of metallurgy and welding in the past decade, the Committee which has been delegated the responsibility for writing specifications for welding filler metal chemistry is faced with the problem of setting rather definite limits with less quantitative data available than would be desirable. Such a situation results in a studied compromise by the Committee between the wide limits desired from the standpoint of ease of manufacture to these specifications and the narrow limits desired to make the properties of all materials which meet the specification as nearly constant as possible.

Since many of the decisions made in this regard are of necessity arbitrary, it was felt desirable to record the considerations upon which the chemical limits set forth in the specification are based for the following reasons.

1. To facilitate revision of these specifications when additional data indicates the desirability of such a revision.
2. To facilitate revision of comparable specifications for covered arc-welding electrodes.
3. To facilitate the acceptance of bare filler metals of modified composition limits where this is necessary in special applications not considered in these specifications.
4. To assist in establishing the risk involved in accepting bare filler metals outside of these specifications.

In the case of the chromium and chromium-nickel corrosion-resistant steels the sensible point for the development of an electrode specification should be the comparable specifications for base metal of the same grades. Tables 1 and 2 provide a comparison between the AISI specifications and the AWS-ASTM specifications for bare electrodes and welding rods. Since the behavior of materials of the same chemistries should be the same in weld deposits as in base metals the deviation in the chemistry of the weld deposits must be explained on the basis of changes in chemistry in the transfer of metal to the weld or in other special considerations required in welding to achieve a sound weld or a weld of satisfactory properties in view of the microsegregation and cast structure present in the weld deposit.

In order to simplify the presentation, the deviations in chemistries of the chromium-nickel and chromium grades will be discussed separately.

CHROMIUM-NICKEL STAINLESS STEELS

The earliest attempts to weld with filler metals of compositions similar to the chromium-nickel austenitic steel base metals resulted either in micro or macro cracking. On an almost empirical basis, it was found that those compositions with lower nickel contents and higher chromium contents exhibited a much lesser tendency toward cracking than the normal compositions. Micro examination of weld deposits of the crack-free welds showed them to contain small percentages of a second constituent, ferrite, in the austenitic matrix.

The metallurgical phenomena involved in the formation of the duplex ferrite-austenite structure of these welds is quite involved but it is of sufficient importance to warrant a brief discussion. Pure iron solidifies first as ferrite and upon cooling transforms to austenite and

Prepared by Subcommittee IV—High-Alloy Steel Filler Metal AWS-ASTM Committee of Filler Metal.

Table 1—Comparison of Base Metal and Bare Filler Metal Chemical Requirements for Chromium-Nickel Steels

	*C**	*Cr*	*Ni*	*Mo*	*Si**	*Mn**	*P*	*S*	*Nickel equivalent†* (*Ni*)	*Chromium equivalent‡* (*Cr*)
ER308	0.08	19.5–22.0§	9.0–11.0	...	0.60	1.0–2.5	0.03	0.03	11.0 –14.65	19.95–22.90
AISI 308	0.08	19.0–21.0	10.0–12.0	...	1.00	2.0	0.04	0.03	12.0 –15.40	19.45–22.5
ER308L	0.03	19.5–22.0§	9.0–11.0	...	0.60	1.0–2.5	0.03	0.03	10.1 –13.15	19.95–22.90
AISI 308L	0.03	19.0–21.0	10.0–12.0	...	1.00	2.0	0.04	0.04	11.1 –13.9	19.45–22.5
ER309	0.12	23.0–25.0	12.0–14.0	...	0.60	1.0–2.5	0.03	0.03	14.0 –18.85	23.45–25.90
AISI 309	0.20	22.0–24.0	12.0–15.0	...	1.00	2.0	0.04	0.03	14.9 –22.0	22.45–25.5
ER310	0.08–0.15	25.0†	20.0†	...	0.60	1.0–2.5	0.03	0.03	23.65–27.75	25.45–28.0
AISI 310	0.25	24.0–26.0	19.0–22.0	...	1.50	2.0	0.04	0.03	22.5 –30.5	24.75–28.25
ER316	0.08	18.0–20.0	12.0–14.0	2.0–2.5	0.60	1.0–2.5	0.03	0.03	14.0 –17.65	20.45–23.4
AISI 316	0.10	16.0–18.0	10.0–14.0	2.0–3.0	1.00	2.0	0.04	0.03	12.0 –18.0	18.75–22.5
ER316L	0.03	18.0–20.0	12.0–14.0	2.0–2.5	0.60	1.0–2.5	0.03	0.03	13.10–16.15	20.45–23.4
AISI 316L	0.03	16.0–18.0	10.0–14.0	2.0–3.0	1.00	2.0	0.04	0.03	11.10–15.90	18.75–22.5
ER317	0.08	18.5–20.5	12.0–14.0	3.25–4.0	0.60	1.0–2.5	0.03	0.03	14.0 –17.65	22.20–25.4
AISI 317	0.10	18.0–20.0	12.0–14.0	3.0–4.0	1.00	2.0	0.04	0.03	14.0 –18.0	21.75–25.5
ER330	0.15–0.25	15.0–17.0	34.0†	...	0.60	1.0–2.5	0.03	0.03	39.0 –44.75	15.45–17.90
AISI 330	0.25	14.0–17.0	33.0–37.0	...	1.00	2.0	0.04	0.03	36.5 –45.50	14.75–18.50
ER347	0.08″	18.5–21.0§	8.5–10.5	...	0.60	1.0–2.5	0.03	0.03	10.5 –14.15	19.20–22.40
AISI 347	0.08″	17.0–19.0	9.0–12.0	...	1.00	2.0	0.04	0.03	11.0 –15.40	18.0 –21.0

* Denotes maximum permissible values unless range is shown; for computation of equivalents, reasonable values for the minima have been used.
† Denotes minimum permissible percentages; for computations of equivalents, a range of approximately 2% has been used.
‡ Computation as indicated by the Schaeffler Diagram (Figure 1).
§ Chromium/nickel ratio 1.9/1 minimum.
″ Columbium 10XC minimum, 1.00% maximum.

Table 2—Comparison of Base Metal and Bare Filler Metal Chemical Requirements for Chromium Steels

	C	*Cr*	*Ni*, max	*Mo*, max	*Mn*, max	*Si*, max	*S*, max	*P*, max
ER410	0.07–0.12	12.0–14.0	0.6	0.6	0.6	0.50	0.03	0.03
AISI 410	0.15 max	11.5–13.5			1.0	1.0	0.03	0.04
ER420	0.25–0.40	12.0–14.0	0.6	...	0.6	0.50	0.03	0.03
AISI 420	0.15 min	12.0–14.0			1.0	1.0	0.03	0.04
ER430	0.10 max	15.5–17.0	0.6	...	0.6	0.50	0.03	0.03
AISI 430	0.12 max	14.0–18.0			1.0	1.0	0.03	0.04
ER502	0.10 max	4.5–6.0	0.4	0.45–0.65	0.6	0.60	0.03	0.03
AISI 502	0.10 max	4.0–6.9			1.0	1.0	0.03	0.04

again at lower temperatures the austenite transforms to ferrite, the phase stable at room temperature. Additions of nickel, carbon and manganese to iron tend to suppress the lower temperature transformation from austenite and when the percentages of these elements are sufficiently high the transformation may be suppressed to subzero temperatures producing an austenitic steel. The addition of chromium, molybdenum, silicon, titanium, columbium and tungsten to iron tend to suppress the high temperature transformation of ferrite to austenite and when sufficient percentages of these elements are present, the transformation may be completely suppressed, producing a ferritic steel. Thus a proper balance of nickel, carbon and manganese with chromium, molybdenum, silicon, titanium, columbium and tungsten may produce a weld deposit which may consist of either austenite or ferrite or a mixture of the two phases in any proportion.

The quantitative effect of these elements in this regard is well shown in the Schaeffler diagram presented in Fig. 1. The approximate structure of a weld metal composition may be determined by locating the composition on the diagram after multiplication of the percentages of the elements by the proper factor.

The chemical compositions limited by the AWS-ASTM bare electrode and welding rod specification are shown in Figs. 2–7, together with the comparable base metal specifications. When welding AISI steels with similar filler metals, the resulting weld metal composition and hence its microstructure is influenced by the base metal composition as well as the filler metal composition. Any changes in composition which take place during fusion, especially those encountered in submerged arc welding, must also be considered in using the diagrams to predict approximate weld compositions and microstructures.

It will be observed that the base metals for the most part tend to fall more in the austenitic region, whereas the weld metal compositions meeting ER308, ER308L and ER347 must contain some ferrite*. This has been accomplished by requiring higher chromium and lower nickel contents. Additional assurance of the presence of ferrite is obtained by restriction of the Cr/Ni ratio to 1.9 minimum which in effect cuts off the upper left-hand corner of the diagram.

* No figures have been included for the ER308L and ER316L compositions because, as can be seen in Table 1, the chromium equivalents are identical with the ER308 and ER316 compositions; and nickel equivalents are only slightly lower as a result of the lower carbon maximum.

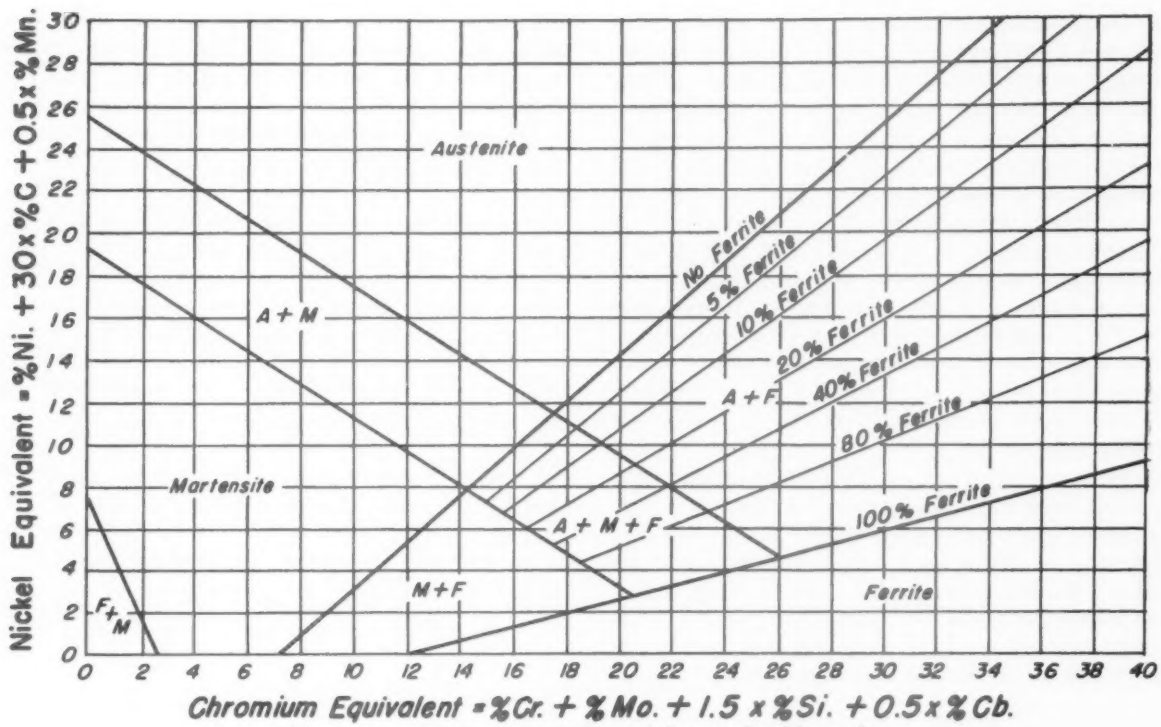

Fig. 1 Constitution diagram for stainless steel weld metal

The lower limits on nickel and the upper limits on chromium are restricted in order to control the maximum ferrite present in the weld deposit. Increased percentages of ferrite in the weld deposit may be expected to have the following effects:

1. Since ferrite is magnetic the permeability of the weld deposit will increase.

2. The ferrite phase has a tendency to transform to the brittle sigma phase during heat treatment in the range of 1300 to 1600° F or during service at temperatures above 1000° F with a resultant loss in ductility. Some experience has been reported with embrittlement in highly ferritic weld deposits at temperatures as low as 800° F similar to the embrittlement in the straight chromium steels in this range. The embrittlement resulting from both these phenomena increases with increasing percentages of ferrite.

3. Since the corrosion resistance of the chromium-nickel austenitic steels is due to the formation of a continuous adherent chromium oxide film on the surface and is not greatly dependent upon the steel being austenitic, the presence of ferrite does not adversely affect the corrosion resistance as measured by the Strauss and Huey tests. One case has been encountered, however, where pitting of the ferrite has occurred in a mildly corroding medium.

For the above reasons the committee endeavored to limit the ferrite content to a maximum value by control of chemistry within commercially acceptable limits.

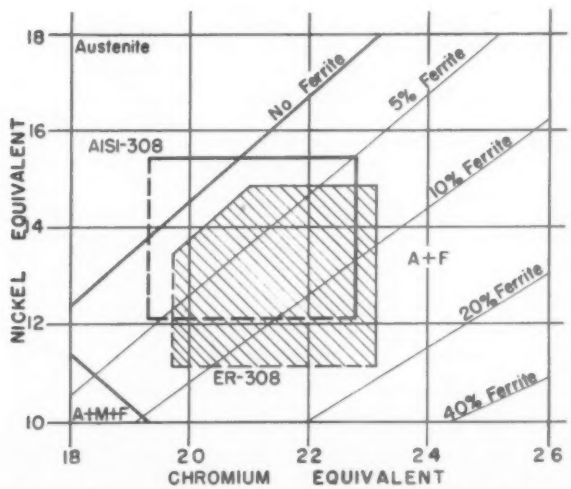

Fig. 2 Constitution diagram for stainless steel weld metal

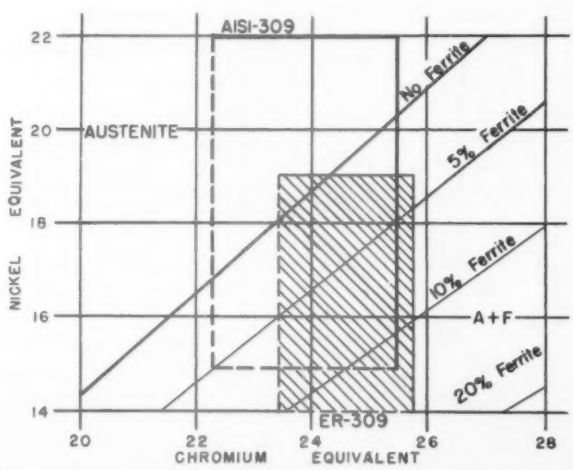

Fig. 3 Constitution diagram for stainless steel weld metal

In the molybdenum containing alloys ER316, ER-316L, and ER317 the specifications permit both fully austenitic and partially ferritic weld deposits. While this is in contradiction to the thesis that the presence of ferrite is important in preventing weld cracking, it has been observed that the presence of molybdenum tended to inhibit cracking even in the fully austenitic compositions of these electrodes.

It may be noted in Fig. 4 that the ER310 compositions have structures consisting only of austenite. This is also true of ER330 compositions. It is obvious from Fig. 4 that prohibitive alterations in composition would be necessary to provide partially ferritic welds and, as might be expected, welds of these compositions are more prone to cracking than the electrodes discussed previously.

Experience with welds of the fully austenitic type have indicated that it is desirable to maintain a high manganese content and a low silicon content and that a low ratio of silicon content to carbon content is desirable. While the reasons for the effectiveness of a high manganese content in preventing cracking is not fully understood, it may be postulated that its effectiveness is due to the elimination of grain boundary sulfide films. It has been proposed that low silicon contents and a low silicon to carbon ratio prevent the formation of low melting silicate films which may also form in grain boundaries and cause cracking.

In accordance with the above, the carbon contents of ER310 and ER330 are higher than permitted in the other electrode specifications. It may be noted that a uniformly higher manganese and a lower silicon content is required for all the chromium-nickel electrodes than for comparable base metal specifications. This results from the desire to minimize cracking in the partially ferritic grades as well as in ER310 and ER330 in the event that base metal dilution causes sufficient composition change to make the structure of the normally partially ferritic grades fully austenitic.

While it would also be desirable from the standpoint of weld cracking to permit higher carbon contents in the ER308, ER309, ER316, ER317 and ER347 grades, higher carbon contents would result in a lower corrosion resistance because of the localized depletion of chromium caused by the precipitation of chromium carbides. Because of the above consideration the car-

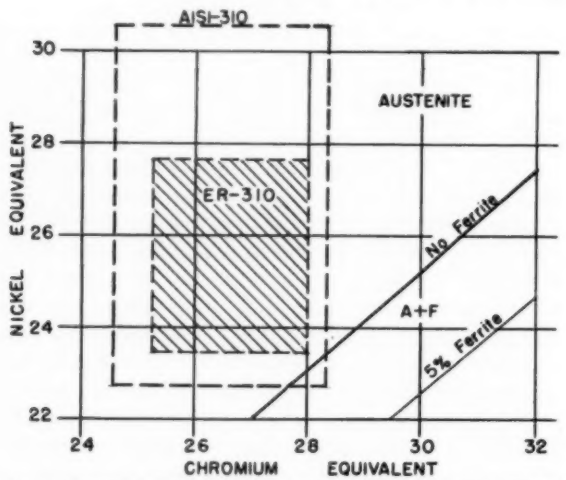

Fig. 4 Constitution diagram for stainless steel weld metal

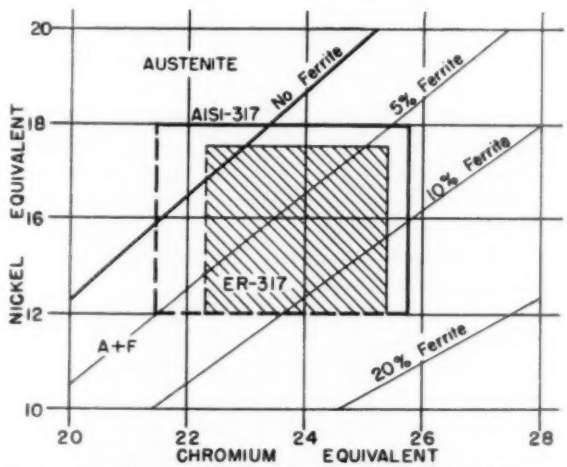

Fig. 6 Constitution diagram for stainless steel weld metal

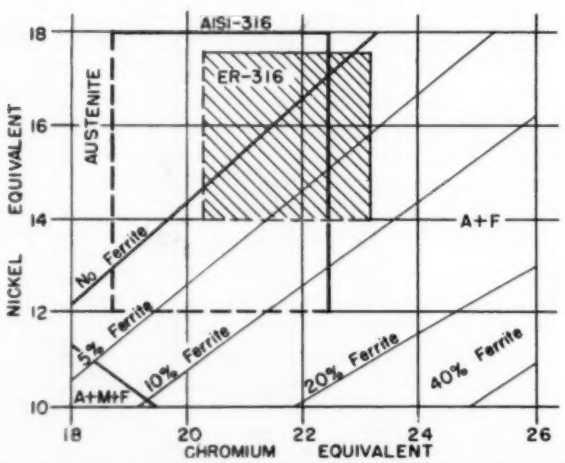

Fig. 5 Constitution diagram for stainless steel weld metal

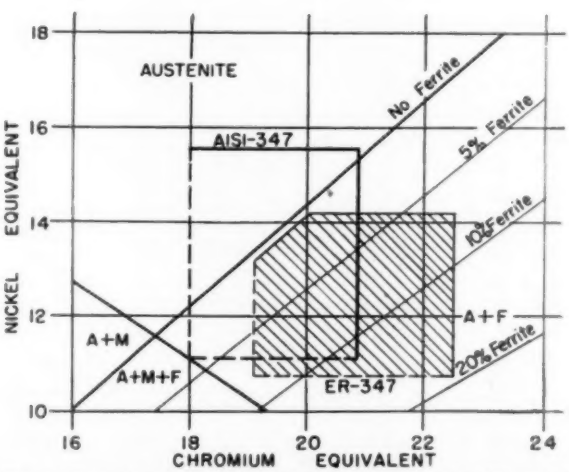

Fig. 7 Constitution diagram for stainless steel weld metal

bon contents of ER316 and ER317 are lower than those permitted by the base metal specification.

CHROMIUM STEELS

The straight chromium corrosion-resisting steels are usually divided into two classes based on their behavior during heat treatment. The martensitic grades are ER410, ER420 and ER502 which transform to austenite upon heating and back to hard martensite on air cooling. Type ER430 is considered a ferritic grade. Though its structure contains some martensite, on air cooling the major constituent is delta ferrite which exhibits no allotropic change on heating and cooling.

In Table 2 it will be noted that the filler metal compositions are essentially the same as for the comparable base metals with a few exceptions:

1. The carbon contents for all except the ER420 grade are kept low so as to minimize the hardness and therefore weld cracking tendencies of these alloys when insufficient preheat is used. For ER420, the carbon is required to provide higher hardness than can be obtained with ER410 but more care is needed in preheating and heat treatment to avoid weld cracks.

2. The chromium contents are practically identical to AISI ranges except in ER430 where a very restricted range is specified. Since ER430 is a borderline alloy its corrosion and impact properties are sensitive to change in composition, especially chromium. If chromium is permitted in the low portion of the range given by AISI the corrosion resistance will suffer; high chromium deposits tend to show reduced impact properties.

3. The nickel is a residual and while it is not specified by AISI, many ASTM specifications provide maximum limits as shown in these specifications.

4. Molybdenum is an essential element in ER502 since it provides the creep strength usually demanded of this grade in service and minimizes embrittlement at elevated temperatures. There has been a difference in opinion as to the need for molybdenum in ER410, some holding that some high temperature applications require this element and since it does no harm it should be mandatory. By specifying a maximum, the grade can be supplied with or without molybdenum. For users requiring high-temperature strength a minimum of 0.40 molybdenum should be specified.

5. Manganese and silicon contents are controlled to slightly lower maximum values than permitted by AISI since experience has shown less welding difficulties when these elements are kept low.

GENERAL

These specifications are considered applicable to filler metals for use in a wide variety of welding processes. It should not be inferred, however, that the use of materials conforming to these specifications will in any way relieve the user of the need to conduct sufficient tests to demonstrate the acceptability of the weld for a particular application itself. Materials specifications can be nothing more than one link in the chain which supports a satisfactory manufacturing controls system.

In some welding processes, in some base metal compositions or in some ranges of base metal size or thickness borderline conditions may prevail, and frequently filler metals play an important role in determining whether or not the final weldment is accepted or rejected. In such borderline cases, it is easily possible for two compositions of filler metal, both meeting the requirements of these specifications, to result in acceptable performance in one case and rejectable performance in the other.

These specifications, like any materials specifications, must not be considered the sole means of control of weldment properties. Qualification, supervision and inspection must be used to provide proper quality control of the product.

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Shielded-Metal-Arc Cutting and Grooving

Principles of shielded metal-arc cutting and grooving; characteristics and development of special coatings; cutting and grooving of mild steel, stainless steel, cast iron and copper; effects of electrode diameter, current, cutting or grooving speed

by Helmut Thielsch and Joseph Quaas

PRINCIPLES

THE primary function of the coating is to stabilize and concentrate the arc onto the area to be cut or grooved. The coating has dielectric properties and decomposes more slowly than the core wire. During the cutting or grooving process the coating forms a crater which extends beyond the core wire by about $^1/_8$ to $^3/_{16}$ in. For four different grooving electrodes this is illustrated in Fig. 1. Because of these characteristics, the electrodes can be inserted into holes, gaps or grooves without short circuiting. In piercing operations, the electrode actually can be "pushed" through the metal without "freezing."

INTRODUCTION

In recent years many new procedures have been developed for the cutting of metals. Most of them have been discussed in numerous papers. However, one method, although widely used, has not been explored and data relative to its performance characteristics have not been published except for a brief discussion in the WELDING HANDBOOK.* This method employs electrodes with special coatings designed for cutting and grooving. Since the development of these new electrodes has given industry new working tools, this article serves to fill a gap in our technical know-how of metal cutting and grooving.

The high power concentration of the cutting and grooving electrodes results in a very narrow heat-affected zone in the base metal. Since distortion in the heat-affected zone is directly related to the temperature rise, the distortion in shielded metal-arc cutting is small. This makes shielded metal-arc cutting particularly suitable for cutting of sheet materials and light structural sections. However, the process works equally well on heavy sections.

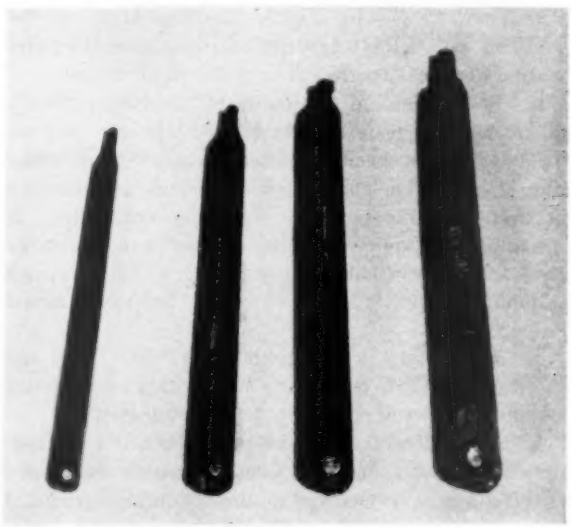


Fig. 1 Grooving electrodes after partial consumption showing "tube" of coating protruding around steel core wire

Fig. 2 Grooving of heavy steel plate

Helmuth Thielsch was Director of Applied Welding Engineering and **Joseph Quaas** is Manager of Electrode Production with the Eutectic Welding Alloys Corp., Flushing, N. Y.

Presented at the Thirty-Fourth National Fall Meeting, AWS, held in Cleveland, Ohio, week of Oct. 19–23, 1953.

* THE WELDING HANDBOOK, Third Edition, Chapter 25, "Arc Cutting," pp. 550–554, 1950, AMERICAN WELDING SOCIETY, New York, N. Y.

The shielded metal-arc cutting and grooving electrodes are used with standard power sources and electrode holders. Although a c may be used, d c straight polarity is generally preferred. The current settings recommended are usually 10 to 50% higher than those for welding with the same diameter mild steel electrodes.

In piercing operations the cutting electrode is held perpendicular or at a very steep angle to the surface. In grooving the chamfering electrode is held at an angle from about 5 to 20 deg—depending upon factors such as the depth of groove desired, the plate thickness, the electrode diameter, etc. For thin plates of less than $^1/_4$ in., a 5-deg angle is usually sufficient. Grooving of a heavy plate is illustrated in Fig. 2.

COATING FORMULATION

The coating formulation of the cutting and grooving electrodes is the result of considerable development to evolve coated electrodes which would not deposit metal. The volume and fluidity of the molten oxidized core wire and parent metal and slag-forming constituents had to exist in a state which would not handicap the welder during the application of the rod. The effect of the cutting or grooving operation on the parent material with respect to the physical properties and corrosion resistance was also considered.

The formulation of the flux coatings presented problems in necessitating ceramic constituents which would withstand the heat encountered during disintegration. Constituents which either provided high oxidizing power or contributed toward balanced exothermic reaction products were utilized within the over-all formulation. The chemistry of such ingredients was important as well as their respective particle sizes, especially since particle size affects the rate of reaction due to varying surface area and influences the burn-off rate and efficiency of operation.

Compatability of each constituent within the final formulation was also considered with respect to chemistry and particle size since producibility of the electrode in the manufacturing plant dictates eventual cost to the consumer.

Although the cutting and grooving electrodes meet the requirements pertaining to their respective end uses, their formulations are quite different analytically. The cutting electrode relies on a higher degree of oxidation than the grooving electrode. The cutting electrode formulation produces a blast created by the arc energy and oxygen from the decomposition of the oxygen-producing constituents. The grooving electrode also relies on these features and in addition releases vast quantities of protective gases at the arcing temperatures. Upon expansion, these gases exert a definite pressure within the enclosure created by the long crater typical of this electrode. Thus, a jet effect is created which virtually blasts molten parent metal and slag out of the path of the electrode.

The cutting electrode has a ceramic coating which is nonconductive and thus prevents shorting. The coating constituents in the grooving electrode are formulated to provide a conductive slag during fusion. This imparts self-starting characteristics to the electrode. This feature enables the welder to preplace the rod in the exact spot where metal is to be removed without instantaneous arcing.

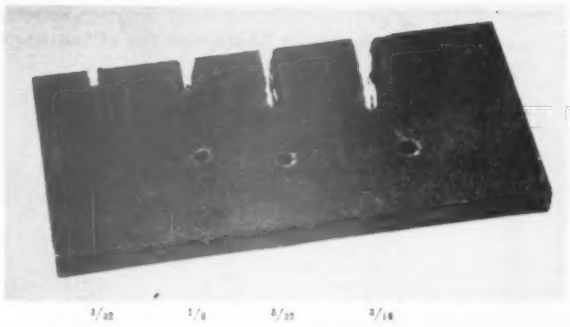


Fig. 3 Appearance of 1-in. thick steel plate after slotting with $^3/_{32}$-, $^1/_8$-, $^5/_{32}$- and $^3/_{16}$-in. diam cutting electrodes

CUTTING ELECTRODES

The relative performance characteristics of our cutting electrode on various materials was determined by cutting slots into the sides of small plates of varying thicknesses. Figure 3 shows the appearance of a 1-in. thick steel plate after the slotting operation with $^3/_{32}$-, $^1/_8$-, $^5/_{32}$- and $^3/_{16}$-in. diam cutting electrodes. Each electrode was run until 10 in. of its length had been consumed. Holes were also pierced through the

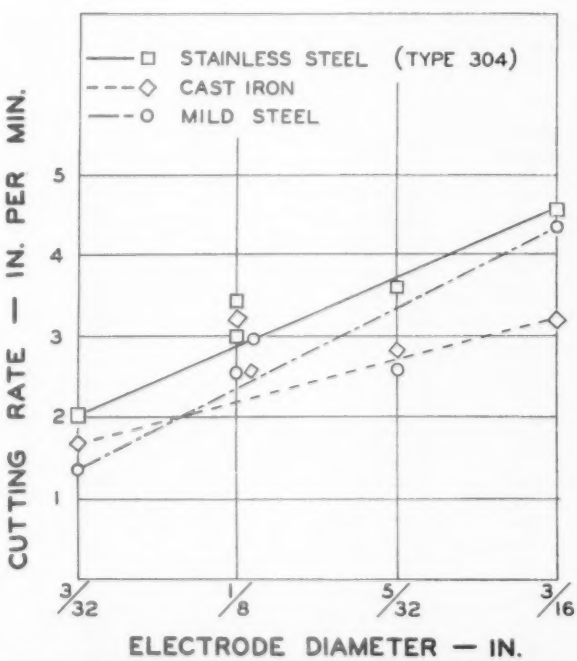


Fig. 4 Relative cutting speed of $^1/_8$-in. diam electrode in the slotting tests of 1-in. thick mild steel, cast iron and stainless steel plates

Table 1—Performance Characteristics of Cutting Electrode on Mild Steels, Cast Iron and Stainless Steels

Electrode diameter in.	*Plate material*	*Plate thickness, in.*	*Current, amp*	*Time, sec*	*Metal removed, oz*	*Cutting rate, ipm*	*Length of cut per 12-in. electrode length*	*Metal removed per pound electrode, lb*
3/32	Mild steel	1	140	16.0	0.38	1.40	0.45	0.93
3/32	Cast iron	1	155	13.0	0.28	1.68	0.45	0.68
3/32	Stainless 304	1	125	16.0	0.5	2.00	0.53	1.24
1/8	Mild steel	1	195	21.5	1.2	2.53	1.09	1.35
1/8	Cast iron	1	220	20.5	1.13	3.11	1.27	1.35
1/8	Stainless 304	1	190	22.5	1.88	3.00	1.35	2.26
5/32	Mild steel	1	215	28.0	1.75	2.68	1.50	1.32
5/32	Cast iron	1	250	23.0	2.03	2.85	1.31	1.55
5/32	Stainless 304	1	235	26.5	2.63	3.60	1.92	2.00
3/16	Mild steel	1	305	29.0	3.5	4.27	2.48	1.98
3/16	Cast iron	1	315	28.0	2.75	3.34	1.88	1.55
3/16	Stainless 304	1	300	28.5	4.25	4.50	2.55	2.39

plates with each diameter electrode. Each test was started on a plate cooled to room temperature.

Test results on 1-in. thick steel, cast-iron and stainless steel plates are given in Table 1. For similar electrode sizes and current settings the efficiency of the cutting electrode was lowest on cast iron and highest on stainless steel. The reasons for this are closely related to the relative fluidity of the molten metal and slag. Under test conditions the molten stainless steel appeared more fluid than either molten mild steel or cast iron. Consequently, stainless steel can be cut at the highest rate. Higher heat in the base metal also im-

Table 2—Performance Characteristics of Cutting Electrode on Copper

Electrode diameter, in.	*Plate material*	*Plate temperature*	*Plate thickness, in.*	*Current, amp*	*Time, sec*	*Metal removed, oz*	*Cutting rate, ipm*	*Length of cut per 12-in. electrode length*	*Metal removed per pound electrode, lb*
3/32	Copper	R. T.	1/2	140	17.0	0.13	0.22	0.08	0.31
1/8	Copper	R. T.	1/2	210	20.0	0.25	1.50	0.60	0.30
5/32	Copper	R. T.	1/2	220	27.5	0.50	1.63	0.90	0.38
3/16	Copper	R. T.	1/2	310	29.0	2.13	4.40	2.56	1.20
5/32	Copper	500° F	1/2	220	32.0	2.50	4.46	2.38	1.90
3/16	Copper	500° F	1/2	325	30.0	4.50	8.00	4.00	2.53
5/32	Copper	800° F	1/2	220	31.0	2.75	5.80	3.00	2.10
3/16	Copper	800° F	1/2	325	31.0	5.25	9.68	5.00	2.95

Table 3—Effects of Plate Thickness Upon Performance Characteristics of Cutting Electrode on Mild Steel and Stainless Steel

Electrode diameter, in.	*Plate material*	*Plate thickness, in.*	*Current, amp*	*Time, sec*	*Metal removed, oz*	*Cutting rate, ipm*	*Length of cut per 12-in. electrode length*	*Metal removed per pound electrode, lb*
3/32	Mild steel	1/4	140	15.5	1.40	8.25	2.13	2.88
3/32	Mild steel	1/2	140	14.0	1.00	4.82	1.13	2.08
3/32	Mild steel	3/4	140	15.5	0.75	2.42	0.75	1.55
3/32	Mild steel	1	140	16.0	0.38	1.40	0.45	0.78
1/8	Mild steel	1/4	190	26.0	2.00	9.25	4.00	1.96
1/8	Mild steel	1/2	190	25.0	1.75	5.40	2.25	1.71
1/8	Mild steel	3/4	190	22.5	1.38	3.33	1.09	1.35
1/8	Mild steel	1	190	21.5	1.20	2.53	0.75	1.18
5/32	Mild steel	1/4	220	31.0	2.96	10.20	5.25	1.93
5/32	Mild steel	1/2	220	31.0	2.50	6.30	3.25	1.62
5/32	Mild steel	3/4	220	28.0	2.00	4.55	2.55	1.30
5/32	Mild steel	1	220	27.0	1.75	3.00	1.75	1.14
3/16	Mild steel	1/4	325	30.0	4.20	13.50	6.75	1.97
3/16	Mild steel	1/2	325	34.0	3.80	8.60	4.88	1.78
3/16	Mild steel	3/4	325	28.5	3.50	5.67	3.22	1.64
3/16	Mild steel	1	305	29.0	3.50	4.27	2.48	1.64
3/32	Stainless 304	1/4	140	26.0	0.91	8.06	3.50	1.88
3/32	Stainless 304	1/2	130	18.0	0.75	2.91	1.05	1.54
3/32	Stainless 304	1	125	16.0	0.50	2.00	0.53	1.09
1/8	Stainless 304	1/4	190	24.0	2.10	12.50	5.00	2.06
1/8	Stainless 304	1/2	210	21.0	1.88	5.73	2.40	1.84
1/8	Stainless 304	1	190	22.5	1.88	3.00	1.35	1.84
5/32	Stainless 304	1/4	210	31.0	3.25	12.60	6.50	2.11
5/32	Stainless 304	1/2	225	29.0	3.00	6.72	3.90	1.95
5/32	Stainless 304	1	235	26.5	2.63	3.60	1.92	1.71
3/16	Stainless 304	1/4	325	31.0	4.40	12.60	6.50	2.06
3/16	Stainless 304	1/2	275	31.0	4.13	7.75	4.80	1.93
3/16	Stainless 304	1	300	28.5	4.25	4.50	2.55	1.99

Table 4—Effect of Current Setting on Performance Characteristics of 1/8-In. Diam Cutting Electrode on Mild Steel

Plate material	*Plate thickness, in.*	*Current, amp*	*Time, sec*	*Metal removed, oz*	*Cutting rate, ipm*	*Length of cut per 12-in. electrode length*	*Metal removed per pound electrode, lb*
Mild steel	3/4	120	31.0	0.76	1.04	0.54	0.75
Mild steel	3/4	140	32.0	1.22	1.92	1.02	1.20
Mild steel	3/4	160	29.4	1.47	2.54	1.25	1.44
Mild steel	3/4	180	26.4	1.71	3.40	1.50	1.68
Mild steel	3/4	200	20.5	1.99	4.40	1.50	1.95
Mild steel	3/4	220	21.0	1.89	5.10	1.63	1.85
Mild steel	3/4	240	19.8	1.64	6.13	2.00	1.60
Mild steel	3/4	260	15.0	1.52	7.25	1.87	1.50
Mild steel	3/4	280	13.0	1.38	7.40	1.57	1.35

proves cutting efficiency. Stainless steel with its relative low heat conductivity thus will retain more heat in the area being cut. Figure 4 illustrates the relation between electrode diameter and cutting speed.

Results on copper are given in Table 2. As evident from the data plotted in Fig. 5 preheating to 500 and 800° F raises the cutting rate and efficiency by approximately 4 to 5 times, respectively.

The effect of plate thickness is illustrated in Table 3. Since heat is more rapidly dissipated in heavy plates than in thin plates, and since a higher heat in the base metal improves the cutting efficiency, the 1/4-in. steel plate will show better results than the 1-in. plate. The relative differences, however, decrease with increasing electrode diameter. Thus, with the heavier 3/16-in. diam electrode the cutting efficiencies are more nearly the same for the various plate thicknesses tested.

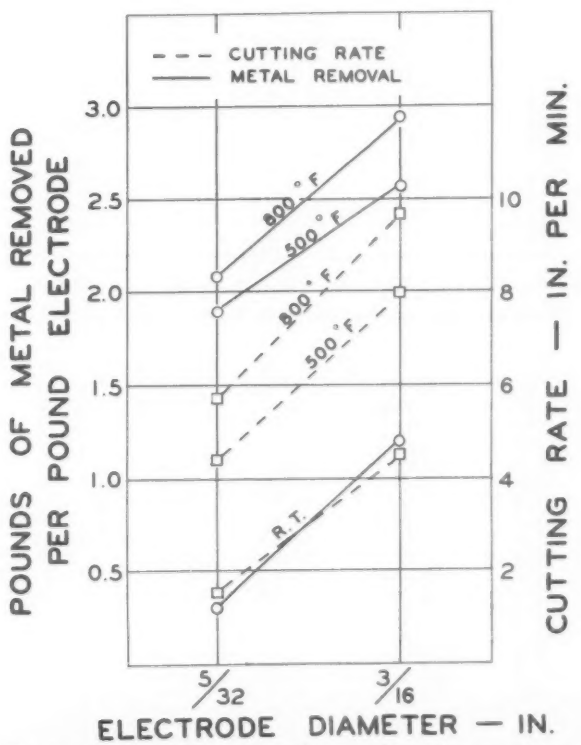


Fig. 5 Cutting efficiency and rate of cutting on 1/2-in. thick copper plates at room temperature and preheats of 500 and 800° F

On stainless steel the effect of plate thickness is considerably less pronounced because the relatively low heat conductivity of the material minimizes the rate of heat dissipation away from the area being cut.

The effect of current for a 1/8-in. diam cutting electrode used in steel is given in Table 4. Whereas approximately 200 amp would be considered normal for this electrode, tests were made also with amperages ranging between 120 and 280 amp. The results are more apparent from the graph shown in Fig. 6. Theoretically, the amount of metal removed should increase proportionally to the cutting speed. However, at a current above approximately 200 amp the electrode coating becomes overheated and loses its operating efficiency. Thus for the 1/8-in. diam cutting electrode, the greatest cutting efficiency is obtained at a current of approximately 200 amp. These results also demonstrate the importance of special coating formulation.

GROOVING ELECTRODES

The relative performance characteristics of our grooving electrode were determined by "pushing" the

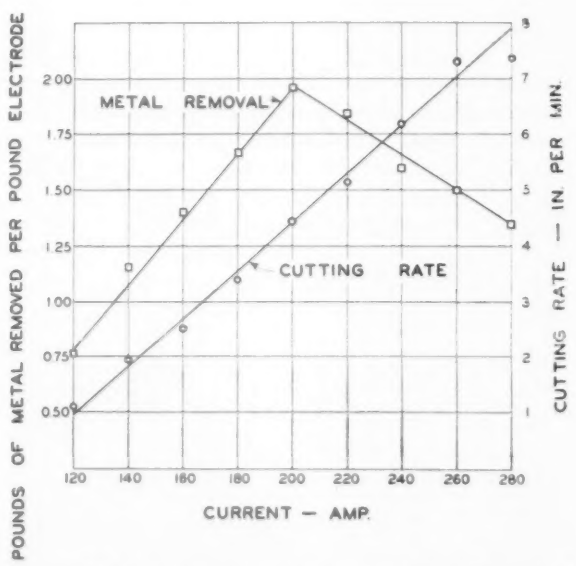


Fig. 6 Effect of current setting on cutting efficiency of 1/8-in. diam cutting electrodes used in slotting tests on steel

Table 5—Performance Characteristics of Grooving Electrode on Mild Steel, Cast Iron and Stainless Steel

Electrode diameter, in.	Plate material	Plate thickness, in.	Voltage	Current, amp	Time, sec	Depth of groove, in.	Width of groove, in.	Length of electrode consumed, in.	Metal removed, oz.	Grooving rate, ipm	Metal removed per pound electrode, lb
3/32	Mild steel	1	58	100	14.0	1/16	3/16	5.75	0.75	24.6	2.19
3/32	Cast iron	1	63	100	15.0	...	...	6.13	0.63	24.4	1.70
3/32	Stainless 304	1	60	110	11.5	...	...	4.0	0.25	20.8	1.04
1/8	Mild steel	1	73	170	13.5	3/32	5/16	5.31	1.0	23.6	1.54
1/8	Cast iron	1	73	160	16.0	...	...	6.0	0.75	22.5	1.02
1/8	Stainless 304	1	71	175	11.5	...	...	4.63	0.63	24.2	1.10
5/32	Mild steel	1	72	230	13.5	1/8	3/8	4.63	1.25	20.6	1.54
5/32	Cast iron	1	71	250	13.5	...	...	5.25	1.5	23.2	1.62
5/32	Stainless 304	1	74	225	10.5	...	...	3.5	0.75	20.0	1.21
3/16	Mild steel	1	69	275	16.75	5/32	7/16	3.88	1.75	13.9	1.90
3/16	Cast iron	1	69	290	12.0	...	...	3.38	1.25	16.8	1.56
3/16	Stainless 304	1	68	295	12.0	...	...	2.5	1.0	12.5	1.68

Table 6—Performance Characteristics of Grooving Electrode on Copper

Electrode diameter, in.	Plate temperature	Plate material	Plate thickness, in.	Current, amp	Time, sec	Metal removed, oz	Grooving rate, ipm	Length of electrode consumed, in.	Metal removed per pound electrode, lb
5/32	R. T.	Copper	1/2	220	29.0	0.37	23.8	11.5	0.18
3/16	R. T.	Copper	1/2	270	39.0	0.75	15.0	9.8	0.32
5/32	500° F	Copper	1/2	220	25.0	1.00	20.4	8.5	0.67
3/16	500° F	Copper	1/2	270	36.0	1.50	14.3	7.5	0.85
5/32	800° F	Copper	1/2	220	22.5	1.25	20.0	7.5	0.95
3/16	800° F	Copper	1/2	270	34.0	2.37	10.0	6.0	1.66

Table 7—Effects of Plate Thickness Upon Performance Characteristics of Grooving Electrode on Mild Steel

Electrode diameter, in.	Plate material	Plate thickness, in.	Voltage	Current, amp	Time, sec	Metal removed, oz	Grooving rate, ipm	Length of electrode consumed, in.	Metal removed per pound electrode, lb
3/32	Mild steel	1/4	58	100	15.0	0.13	20.5	5.13	0.41
3/32	Mild steel	1	58	100	14.0	0.75	24.6	5.75	2.19
3/32	Mild steel	2	59	100	21.0	1.0	18.4	6.44	2.17
1/8	Mild steel	1/4	72	160	12.5	0.25	22.8	4.75	0.43
1/8	Mild steel	1	73	170	13.5	1.0	23.6	5.31	1.54
1/8	Mild steel	2	72	170	24.0	1.5	21.8	8.75	1.40
5/32	Mild steel	1/4	71	220	11.5	0.75	21.5	4.13	1.04
5/32	Mild steel	1	72	230	13.5	1.3	20.6	4.63	1.54
5/32	Mild steel	2	71	220	20.0	1.8	20.6	6.88	1.45
3/16	Mild steel	1/4	67	290	9.0	0.5	11.7	1.75	1.30
3/16	Mild steel	1	69	275	16.75	1.8	13.9	3.88	1.90
3/16	Mild steel	2	69	270	23.0	2.5	10.1	3.88	1.65

Table 8—Effect of Angle of Inclination on Grooving Efficiency of Grooving Electrode on Mild Steel

Electrode diameter, in.	Plate material	Plate thickness, in.	Angle of inclination, deg	Current, amp	Time, sec	Metal removed, oz	Grooving rate, ipm	Length of electrode consumed, in.	Metal removed per pound electrode, lb
1/8	Mild steel	1	5.8	170	12.0	0.70	22.5	4.50	1.27
1/8	Mild steel	1	8.7	170	12.0	0.90	22.5	4.50	1.64
1/8	Mild steel	1	11.6	170	13.0	0.90	24.3	5.00	1.64
1/8	Mild steel	1	14.5	170	14.0	1.00	23.6	5.50	1.49
1/8	Mild steel	1	17.5	170	15.0	0.70	23.0	5.75	1.00
1/8	Mild steel	1	20.5	170	15.0	0.50	24.0	6.00	0.68
5/32	Mild steel	1	5.8	220	14.0	1.50	15.0	3.50	2.43
5/32	Mild steel	1	8.7	220	15.0	2.00	15.0	3.75	3.03
5/32	Mild steel	1	11.6	220	18.0	1.75	15.0	4.50	2.20
5/32	Mild steel	1	14.5	220	18.0	1.25	15.8	4.75	1.50
5/32	Mild steel	1	17.5	220	18.0	1.00	16.7	5.00	1.14
5/32	Mild steel	1	20.5	220	19.0	0.85	17.4	5.50	0.88
3/16	Mild steel	1	5.8	320	21.0	2.25	14.3	4.50	2.11
3/16	Mild steel	1	8.7	320	22.0	4.00	15.0	5.00	3.40
3/16	Mild steel	1	11.6	320	26.0	3.75	16.1	7.00	2.27
3/16	Mild steel	1	14.5	320	27.0	3.00	16.1	7.25	1.74
3/16	Mild steel	1	17.5	320	31.0	2.50	15.5	8.00	1.31
3/16	Mild steel	1	20.5	320	32.0	2.00	15.0	8.00	1.06

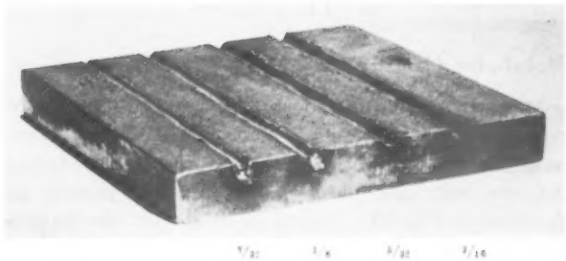


Fig. 7 Appearance of 1-in. thick steel plate after grooving with 3/32-, 1/8-, 5/32- and 3/16-in. diam grooving electrodes

electrode over the metal surface at a low incident angle as shown in Fig. 2. Figure 7 illustrates the appearance of the steel plate after grooving with 3/32-, 1/8-, 5/32- and 3/16-in. diam electrodes. Figure 8 illustrates the U-type profile of the resulting grooves after cross sectioning the plate.

Fig. 8 U-type profile in 1/2-in. plate after grooving with 3/32-, 1/8-, 5/32- and 3/16-in. diam grooving electrodes

Test results on 1-in. thick mild steel, cast-iron and stainless steel plates are given in Table 5. Although heavier electrodes will remove more metal, the pounds

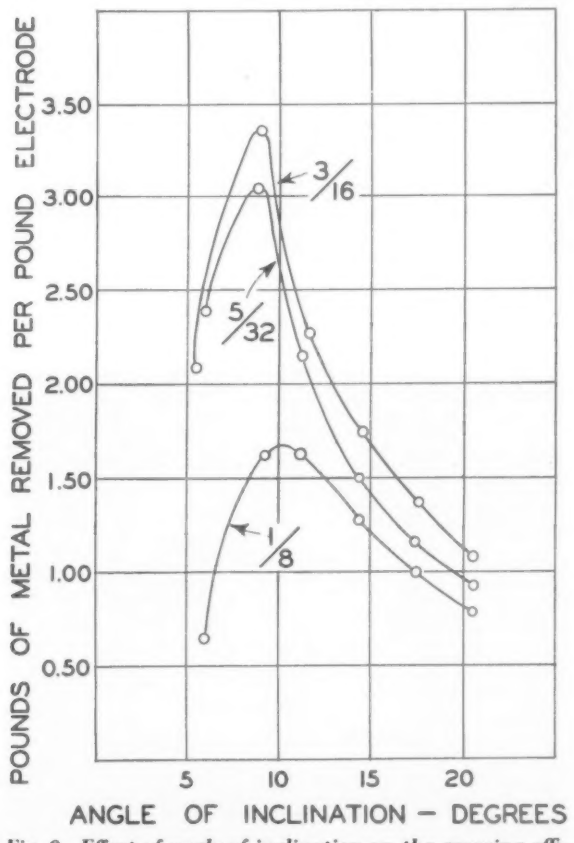


Fig. 9 Effect of angle of inclination on the grooving efficiency of the 1/8-in. diam grooving electrodes on mild steel

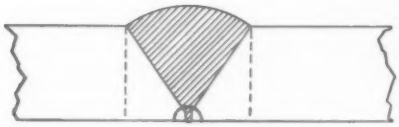


Fig. 10 Sketch illustrating cut made in the removal of weld deposit

of metal removed per pound of electrode is essentially the same for each metal. However, since the heavier electrodes remove more metal per electrode they are more economical in that they reduce the labor cost considerably.

Whereas in cutting the best results are obtained on stainless steel; in grooving, mild steel gives the better results. Because of the blasting action of the grooving electrode the fluidity of the molten metal is inconsequential. On the other hand, oxidation is a factor so that the high melting point of the chromium oxide and the required higher fluxing action of the iron oxide and electrode coating constituents reduce the effectiveness of the electrode on stainless steel as compared to mild steel or cast iron.

Results on copper are given in Table 6. As was true in cutting, preheating of the copper plate to 500 and 800° F improved the efficiency of the electrode by increasing the amount of metal removal by 3 to 5 times over the room temperature results.

The effect of plate thickness is given in Table 7 where test results on 1/4-, 1- and 2-in. thick mild steel plates are summarized. Although it might be expected that on the 2-in. plates the efficiency would be lower than on the 1/4-in. plates, the reverse occurs due to the human element involved. On thin plates the welder tends to be afraid of cutting through the plates and for that reason he will use an incident angle of about 5 deg—as occurred in these tests. On the 1-in. plates he used a 10-deg angle—resulting in more base metal being grooved out. The 5-deg angle used on the 1/4-in. plates results in a greater percentage of arc energy being dissipated into the atmosphere. Tests on 2-in. plates made with an angle of 10 deg showed a slight decrease in the amount of metal removed, due to the greater amount of heat absorption by the heavier base metal as compared to the tests on the 1-in. thick plate.

In Table 8 and Fig. 9 are given the effects of angle between the grooving electrode and the base metal plate (mild steel). Best results are obtained when the

Fig. 11 Appearance of steel section after removal of stud with cutting electrode

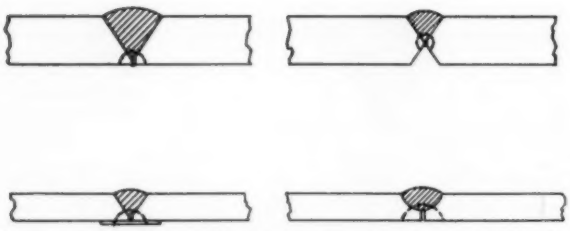


Fig. 12 Sketches illustrating application of "back-chipping" with grooving electrode

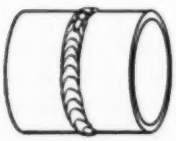


Fig. 13 Sketch showing the grooving out of defects in welded pipe section

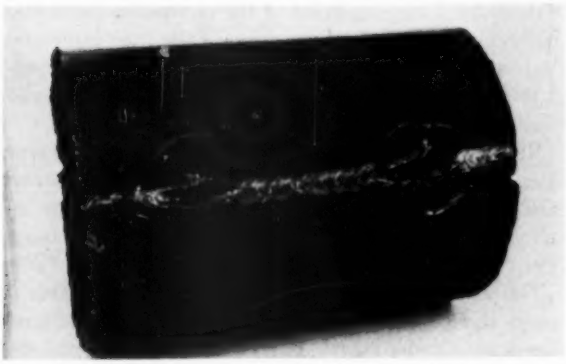


Fig. 14 Example of grooved out weld defects in pipe section

electrode is held at an angle of approximately 10 deg. A lower angle, as mentioned earlier, results in excessive dissipation of arc energy into the atmosphere. Above 10 deg the electrode tends to "dig in" into the plate and the "blowing" action of the arc becomes less effective.

APPLICATIONS

Metal-Arc Cutting

Metal-arc cutting is widely used for cutting holes into piping, through plates for subsequent welding of extensions, attachments, etc. Previously deposited weld deposits may also be removed by this process as sketched in Fig. 10. Figure 11 illustrates the appearance of a steel section after removal of a stud.

For piercing, drilling and cutting operations, the cutting electrode is particularly effective on the difficult to machine metals and alloys. Armor plate, air and deep hardening steels, stainless steels and hard overlay materials are representative of some of the materials easily cut.

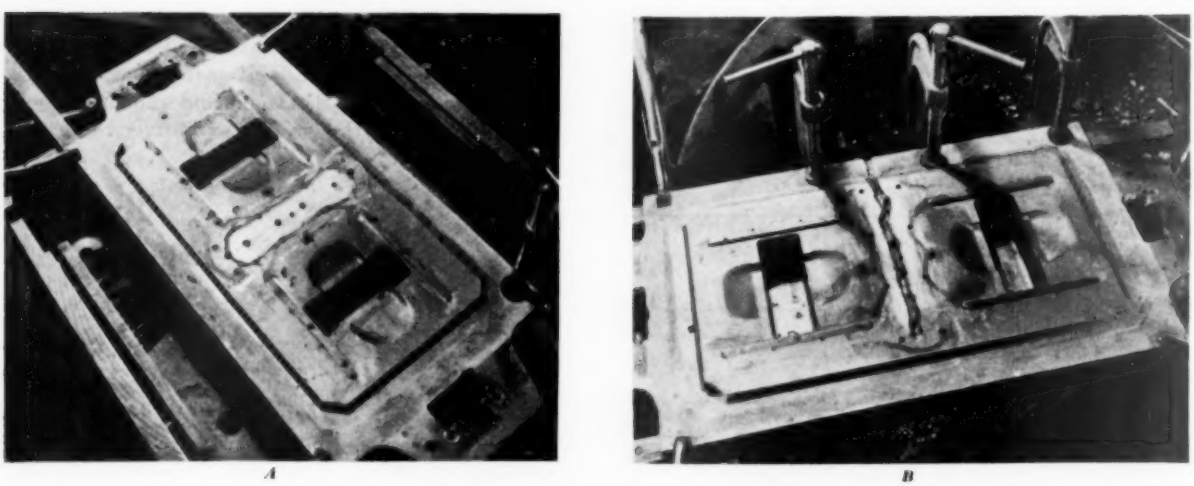
The cutting electrode is also used for the same purposes previously mentioned for underwater operations. For these applications the coated electrode is protected by a moistureproof covering which enhances its efficiency for underwater work. Fully insulated holders are required for underwater applications and d-c straight polarity power must be used to prevent electrolytic corrosion of the holder and parts of the diver's equipment.

In ferrous and nonferrous foundries, the cutting electrode is used for the removal of risers and gates and to cut up heavy scrap for remelting.

Metal-Arc Grooving

Metal-arc grooving is extensively used for preparation for welding such as grooving for butt or fillet welds. It is also used with excellent results as a "back-chipping" tool in applications similar to those sketched in Fig. 12.

In production welding, especially weldments requiring excellent X-ray quality standards, the grooving electrode is used for removal of weld metal which radio-



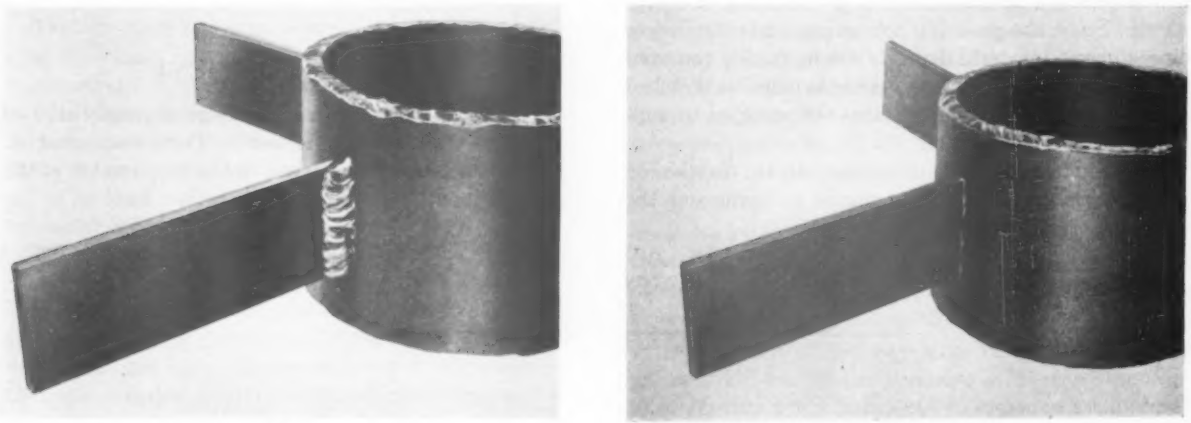
A *B*

Fig. 15 Broken match plate in aluminum foundry pattern before, A, and after, B, grooving out of cracked area

Fig. 16 Sketch illustrating removal of weld deposits in lap and Tee joints

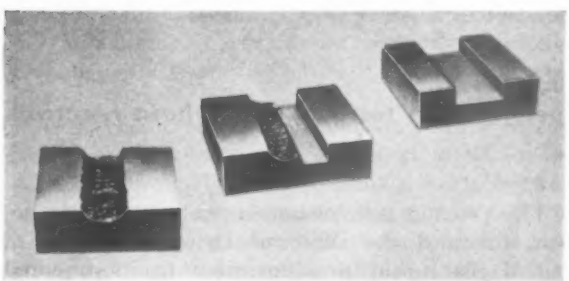
graphs showed to be porous, cracked or otherwise defective. The job inspector can mark areas to be removed and rewelded. Under these conditions the welder can utilize the same power equipment used for welding, grooving out poor welds and then immediately rewelding.

In maintenance, cracks in iron and steel castings, forgings or weld deposits are readily grooved out for



A *B*

Fig. 17 Example of removal of support lugs before, A, and after, B, grooving out of welds



A *B* *C*

Fig. 18 Example of "premachining" with grooving electrode and subsequent "finish" milling of slot in steel plate

subsequent rewelding. By grooving out the crack, the electrode provides a U groove for subsequent rewelding in a fraction of the time required by mechanical chipping or grinding. Examples of grooved out defects in welded pipe sections are given in Figs. 13 and 14. Smearing over of the defect, as may occur with the mechanical methods is also avoided. On heavy cast-iron sections, it may be advisable to drill small holes at the outermost extensions of each crack. Figure 15 illustrates a broken match plate of an aluminum foundry

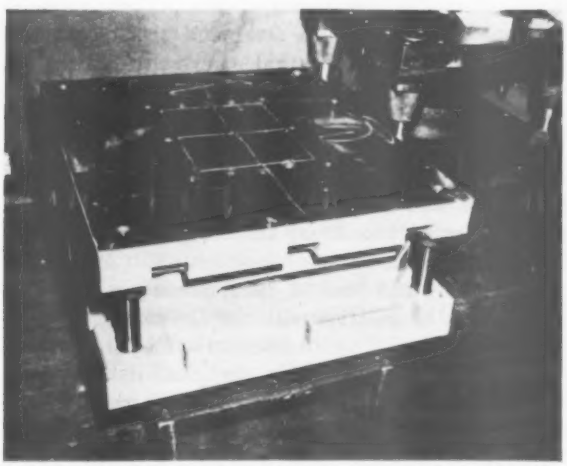


Fig. 19 Grooving of draw dies to connect holes for the escapement of trapped air

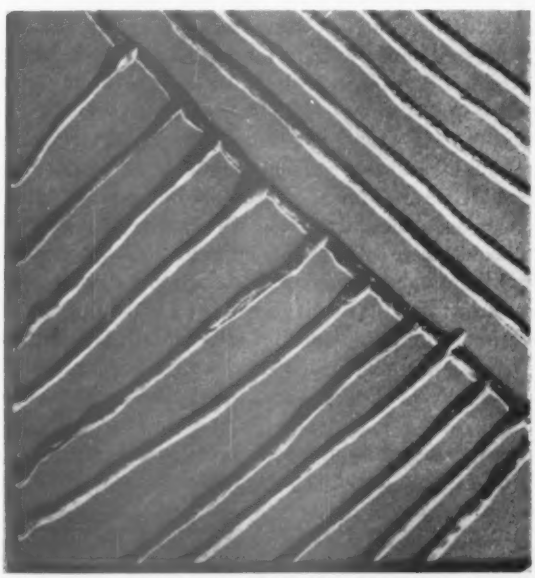


Fig. 20 Grooving of "diamond" plates to provide for nonskid surface

pattern before, *A*, and after, *B*, grooving out of the cracked area.

With these special electrodes repairs can often be made without dismantling or removal of the equipment. This results in considerable saving in man-hours. For example, cracks in motor housings are readily grooved out and rewelded without requiring dismantling or removal of the equipment.

Shielded-metal-arc grooving is used also with excellent success for alterations in or dismantling of equipment. Since the grooving action can be controlled to close dimensions, weld deposits can be readily removed with a minimum effect upon the base metal as sketched in Fig. 16. Figure 17 illustrates the removal of support lugs on a pipe section.

On mild steels and nonferrous metals, machining costs can be reduced by cutting or grooving out the bulk of the metal to be removed. Machining is then employed primarily as a "finishing" operation. Figure 18 illustrates the initial grooving, *A*, and subsequent milling operations, *B* and *C*, for cutting a slot into a steel plate.

Chamfering of surfaces is used, for example, to prepare grooves in draw dies to connect holes for the escapement of trapped air, Fig. 19, and in plates to provide for "nonskid" surfaces, Fig. 20.

ACKNOWLEDGMENT

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Government Standards

How a Federal Specification touched off a chain reaction

by S. P. Kaidanovsky

FEDERAL Specification QQ-S-741 for Steel, Structural (Including Welding) and Rivet; (for) Bridges and Buildings was issued in 1942 and amended three times. The last amendment was in 1946.

This specification classifies structural steel in two types, Type I for riveted, and Type II for welded structures; and two grades: Grade A, for bridges and Grade B for buildings; and in two classes: Class 1, Noncopper steel and Class 2, Copper-bearing steel.

The issuance of Federal specifications, the use of which is mandatory by all Federal Government agencies, is entrusted by law to the General Services Administration. The preparation of Federal specifications is the responsibility of the Standards Division, Federal Supply Service (FSS), General Services Administration (GSA).

The revision and simplification of Federal Specification QQ-S-741 was initiated under the following circumstances:

The Veterans Administration, early in the Korean war, informed the Office of Defense Mobilization (ODM) that it paid "premium prices" for its structural steel whenever welded constructions were involved and the structural steel was specified in accordance with Federal Specification QQ-S-741. The Veterans Administration considered this matter of importance in the conservation of materials program because welded constructions require less steel and the practice of field assembling of structural steel constructions by welding is superseding riveting at a rapidly increased rate.

The ODM fully agreed with the Veterans Administration on the importance of finding means for conserving steel as well as of reducing construction costs, especially in view of the military and industrial defense mobilization then started.

On April 17 and June 11, 1952, meetings were called by ODM in cooperation with the Defense Production Administration (DPA), to discuss the matter with representatives of Government agencies, industry and technical societies. The following organizations were represented at these meetings: The American Society for Testing Materials, American Iron and Steel Institute, AMERICAN WELDING SOCIETY, American Institute

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of Steel Construction and several Government agencies directly involved in construction work.

The subject was given thorough examination from all points of view. While there were expressed divergent views as to the use of field welding in the building industry, it was nevertheless agreed that field welding is and will be of importance in building defense installations, as for example in erecting aircraft hangers or in reconstruction work in emergency situations, such as after air attacks.

The participants in the discussion agreed that weldable steel may be used for riveting without any discrimination. Finally, all participants agreed that the structural steel as specified in the ASTM A-7 Tentative Specification for Steel for Bridges and Buildings and those in the Federal Specification QQ-S-741 "are identical or at least sufficiently so to permit using them interchangeably."

This brought immediately to the forefront the question of the justification for "premium prices" on weldable structural steels of Type II, Federal Specification QQ-S-741, which premiums averaged in 1952 from $13 to $20 per ton. It was disclosed at the meetings that the chemical characteristics of structural steel in general production runs in steel mills are of a kind suitable for field welding and if the chemical requirements of the steel would be included in the ASTM A-7 Specification, as is the case in the Federal Specification QQ-S-741, the "premium prices" would be considerably reduced and would cover only the specific inspection and packing costs usually involved in Government contracts.

At the end of the meeting of June 11, 1952, it was decided to request the ASTM to prepare at the earliest possible date requirements covering a weldable grade of steel for general building construction for inclusion in the ASTM A-7 Specification, and ask the ASTM to proceed at once on this assignment.

In spite of the fact that the representatives of Subcommittee 2, Structural Steel, of the Committee A-1: Steel, of the ASTM, attended to the organization of these meetings and were present at both meetings, the board of Subcommittee 2 was reluctant to impose on its membership a decision for the modification of the ASTM A-7 Specification in line with these recommendations. Instead it has suggested that the interested Government agencies should modify and change their requirements so as to effect the sought for reduction in the cost of the structural steel these specify.

At this juncture the American Welding Society suggested having a meeting with the representatives of the ODM and interested Government agencies. The subject for discussion was the Society's plan for a revision of the Federal Specification QQ-S-741 by inclusion in it of all test and inspection requirements from ASTM A-7 Specification, all dimensional requirements of ASTM A-6 Tentative Specification for General Requirements for Delivery of Rolled Steel Plates, Shapes, Sheet Piling, and Bars for Structural Use, and a set of requirements for weldable steel which the American Welding Society in collaboration with a leading steel producer and a Government agency satisfactorily worked out for one construction project.

This recommendation when approved by the Federal Government may be then adopted as an American Welding Society (AWS) standard and submitted to Subcommittee 2, Committee A-1 for its consideration and approval.

A meeting was called by the ODM on Oct. 9, 1952, and the consensus of all participants was that an incorporation of the above AWS suggestions in any purchase specification would result in a good specification, and if the AWS would recommend it, and if steel companies would acquiesce, the Government would find it advantageous to revise the existing Federal Specification QQ-S-741 accordingly.

On the basis of these conclusions the ODM called upon GSA for a revision of the Federal Specification QQ-S-741. The Standard Division, FSS, GSA, which conducted an independent study of the problem arrived at practically identical conclusions.

Upon receipt of a request from the ODM for the revision of Federal Specification QQ-S-741, the Standards Division took immediate steps to organize a conference of representatives from all interested Government agencies, and research organizations involved in construction work, and those that use, inspect or conduct research work on weldments, in order to arrive at a directive as to the manner in which the revision of Federal Specification QQ-S-741 should be accomplished.

The conference was held on Jan. 23, 1953, at which time GSA outlined the problem, its background and importance. Since the U.S. Congress had expressed its wish that the executive agencies of the Government should use trade specifications whenever they properly cover Government requirements, it was suggested by GSA that Federal Specification QQ-S-741 be revised by incorporating in it by reference all pertinent requirements from ASTM A-6 and A-7 Specifications supplementing them, in regard to weldable structural steel, with the requirements which were worked out by the American Welding Society in cooperation with a Government agency and one of the leading steel producers.

After prolonged discussions the conference adopted GSA's suggestions and authorized revision of Federal Specification QQ-S-741 on suggested lines for circulation for comments of all Government agencies, trade organizations, individual steel producers and technical and scientific societies. The authorized revised specification was issued as an Interim Federal Specification QQ-S-00741 (GSA-FSS) dated Jan. 29, 1953.

Soon after the issuance of this Interim Federal Specification the American Welding Society's Bridge Code Committee prepared and adopted on May 27, 1953, a specification for steel for welded bridges which incorporated practically verbatim the requirements for weldable steel of the Interim Federal Specification QQ-S-00741.

The American Society for Testing Materials at its annual meeting June 29, through July 3, 1953, in Atlantic City, decided to issue a separate specification for weldable structural steel instead of modifying the ASTM A-7 specification. A proposed specification for a structural welding grade of steel is under preparation. This is based upon the requirements of a welding grade of structural steel adopted by the AMERICAN WELDING SOCIETY's Bridge Code Committee.

The Interim Federal Specification QQ-S-00741 originally issued on Jan. 29, 1953, was revised on July 10, 1953, and submitted for comments to Government agencies, industry and individual producers and fabricators. The revised specification is closer in line with the ASTM A-6 and A-7 specification than the first draft. Physical and inspection requirements from ASTM A-7 and all dimensional requirements from A-6 are now incorporated in the new draft, instead of referencing ASTM specifications as was done in the first draft. The following note is included in the new draft of the specification.

"The specified Type I and Type II steels are basically identical with the steels covered by the ASTM A-7 Specification with the principal distinction that in Type II steel the chemical requirements are restricted and the physical requirements are modified."

Also the title of the specification has been broadened to "Interim Federal Specification QQ-S-00741 (GSA-FSS) for Steel, Structural."

Comments have been received from industry and Government agencies on the July 10, 1953 Interim Federal Specification. These comments will be used in preparing the final draft. There is an *ad hoc* committee of representatives of the Department of Defense General Services Administration and industry which is preparing a draft of a specification for steel, carbon, structural plates, shapes and bars which will be considered in the revision of Interim Federal Specification QQ-S-00741 (GSA-FSS).

This example illustrates how a revision of a Federal Specification touched off a chain reaction, which affected an ASTM Specification and an AMERICAN WELDING SOCIETY Specification. It indicates the interdependence of Government and industry, the necessity for closer cooperation and consultation of mutual technical problems between the two, and adoption of requirments suitable to both, resulting in savings of time and money to Government and industry.

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(Continued on page 458)

Tolerance and Control on Fillet Welds

Welding procedure control to insure suitable joint characteristics in the use of the manually operated semiautomatic submerged-arc welding process

by John Mikulak and Merle Dillman

INTRODUCTION

IN COMPETITIVE industry and, particularly, when it is imperative that production is increased with limited man power, maximum welding man-hour output must be obtained. However, minimum weld physical requirements of various standards set by Code agencies must be maintained to insure product performance. Indirect labor effort must also be reduced to a minimum but not at the expense of inadequate supervision or inspection. Due to the latter, controls are necessary to expedite determination of physical weld joint characteristics after the product has been welded.

The following discussion pertains to establishing a weld procedure control by use of a recording type ammeter and voltmeter in conjunction with the manually operated semiautomatic submerged-arc welding process. This process was selected because of its extremely versatile and simple operating characteristics.

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GENERAL

Fillet welds constitute a major portion of the welding required on fabricated assemblies. In many instances designs requiring corner welds could be more economically produced by fillet welds. Further, the use of the manually operated semiautomatic submerged arc process produces high production speeds with smoother profiles and superior weld joint qualities. Due to its inherent self-control characteristics and less operator skill and fatigue demands, relatively simple and easily adapted controls can be used to assure complete compliance with Code requirements for quality and joint strength.

Currently, common practice for assuring fillet weld size is to determine the leg size by measurement of both legs and further checking for the amount of convexity and concavity. Codes by organizations such as the ASME and AWS determine the strength of the fillet weld by the theoretical throat depth through the weld deposit as shown in Fig. 1. In design calculation the cross-sectional area thru this throat multiplied by the allowable unit stress produces a value indicating the joint strength. The theoretical throat is obtained by constructing the largest right angle triangle which can be inscribed in the cross-sectional area of the weld deposit and then calculating the length of the normal from the vertex of the joint to the hypotenuse of this triangle. The weld size is then established to provide the desired throat dimension.

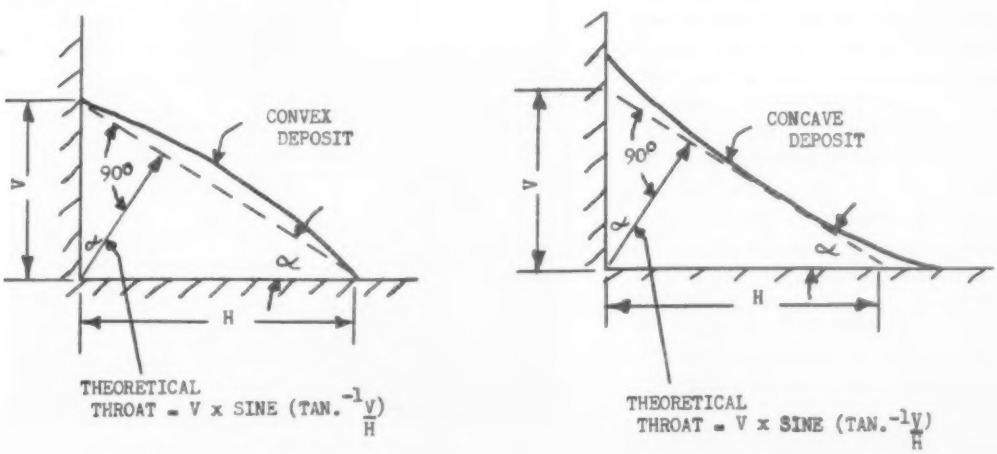


Fig. 1 Conventional method of determining the joint strength of fillet welds

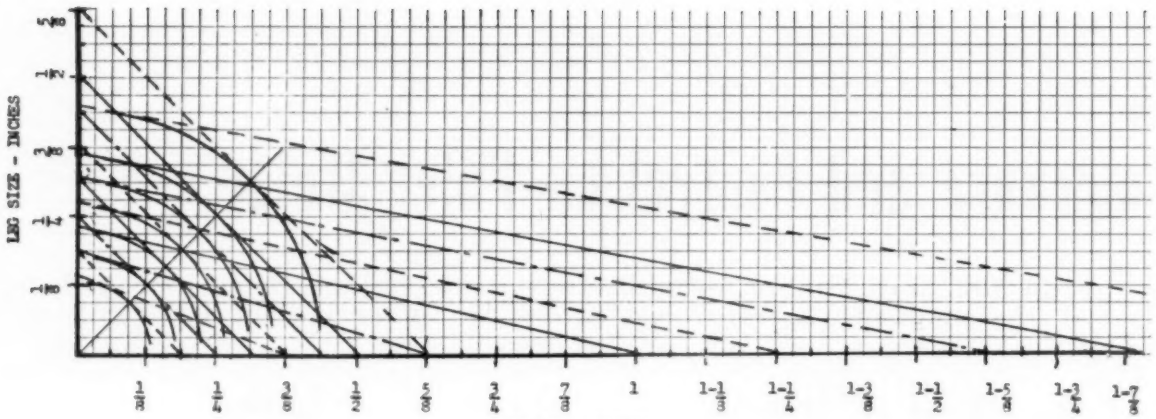


Fig. 2 Fillet weld tolerances, manual submerged arc

Weld size, equal leg	*Theoretical, unequal leg*	*Tolerance*	*Volume increase, %*	*Actual shop, unequal leg*	*Tolerance*	*Volume increase, %*
3/16 x 3/16	9/64 x 3/8	+3/16 −3/64	81.5	5/32 x 9/32	+3/32 −1/32	26
1/4 x 1/4	3/16 x 3/8	+3/8 −1/16	86.3	7/32 x 11/32	+3/32 −1/32	22
5/16 x 5/16	15/64 x 1	+11/16 −3/64	139	9/32 x 13/32	+3/32 −1/32	18
3/8 x 3/8	9/32 x 1 1/4	+7/8 −3/32	150	11/32 x 15/32	+3/32 −1/32	16
7/16 x 7/16	3/16 x 1 3/8	+1 7/16 −7/8	166	3/8 x 17/32	+3/32 −1/16	6
1/2 x 1/2	23/64 x 1 7/8	+1 3/8 −9/64	170	15/32 x 9/16	+1/16 −1/32	6

Unfortunately, because of inadequate procedure control and dubious practices by various organizations and individual welding operators, considerable variation in weld dimensions are obtained when attempting to produce the required throat depth. In applying the rule shown in Fig. 1, considerable theoretical variation could be obtained in weld area for a particular throat depth as is shown on the left side of Fig. 2. Fortunately, in actual practice the total theoretical tolerance is not encountered; however, variations of 50 to 60% over normal area are commonly found.

To the knowledge of the authors little has been done to produce and demonstrate a feasible control on welding joint strength. These controls must take into account variation in power characteristics (line and equipment), joint design, materials, operator skill and should also consider the adaptability of various weld processes.

A complete recommendation is an enormous undertaking requiring considerable energy and, further, a project of this nature should be studied by several organizations or by a group representing a cross section of the industry. In this way full research will be assured to balance prejudices of a single group as well as assuring adaptability to the major applications required in the industry. Consequently, the data in the following are merely presented as evidence that desirable tendencies exist for a possible standard control whereby not only can the weld size be controlled, but also where definite fusion in the parent metal can be guaranteed and can be considered as joint material to enable reducing weld sizes. The latter will expedite sizable cost reductions in weld material and labor as well as increase production output.

A new welding gage is being suggested which improves the accuracy of weld deposit measurement, decreases the time for measuring weld size, and also provides immediate visual means of determining the minimum throat through the weld deposit from which the equivalent weld size can be read on the gage.

TEST SETUP

Figure 3 illustrates the equipment which was used in this test work and consisted of a 600-amp d-c generator with a recording type ammeter and voltmeter (Esterline Anguse Graphic—Model AW). All welding was accomplished with the semiautomatic submerged arc process, manually operated. The ground lead was clamped to each individual specimen and welding progressed away from the ground. Arc welding time was

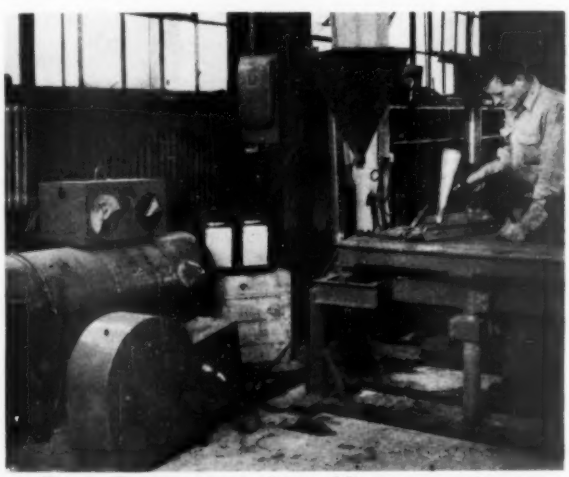


Fig. 3 Equipment used in welding test specimens

obtained with a stop-watch and each weld was measured immediately after welding. In many instances the welding specimen was marked off in 3-in. increments and progress of the arc speed observed for each increment.

Tests were conducted on several plate thicknesses and incorporated several joint fit-ups. Figure 4 illustrates the test specimen which consisted of 3- x 18-in. long bars from hot-rolled steel plate (AISI 1015) of the following thicknesses: $^1/_4$, $^2/_8$, $^1/_2$, $^3/_4$ and $^7/_8$ in. Various joint fit-up conditions were obtained by using shim stock of 0, $^1/_{22}$, $^1/_{16}$ and $^3/_{32}$ in. thicknesses.

The specimens were set for several weld positions and are also shown in Fig. 4 and, on each weld position, three lead angles were used (level, downhill and uphill) as illustrated at the bottom of the latter figure. The purpose of these variations was to make an attempt to encompass all variations possible in production.

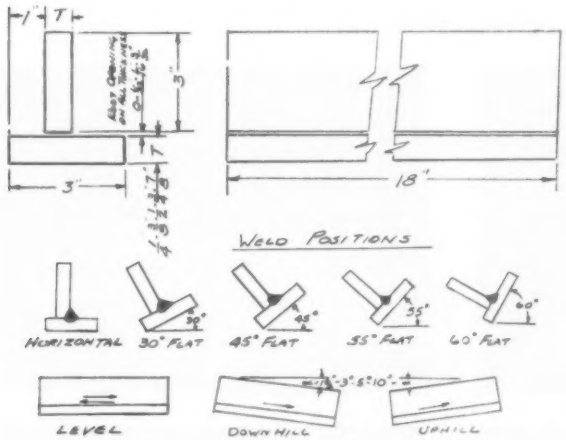


Fig. 4 Test specimens and weld positions

The flux can was manipulated by various techniques, such as oscillating in a circular manner, back and forth in a longitudinal and transverse direction. The can was oscillated by varying amounts from 0 to $^3/_4$ in. when measured at the top of the flux can as illustrated on Fig. 5. The flux can was also held at various lead

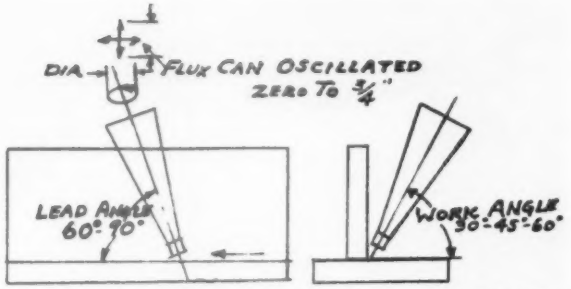


Fig. 5 Diagram showing limits of flux can manipulation

angles from 60 to 90 deg from a horizontal plane. The previously mentioned variations were observed in production shops and, consequently, the purpose of using them was to explore the characteristics of the resulting welds.

Figure 6 illustrates various dimensions which were recorded on the test specimens for flat, convexed and concave fillet welds. The specimens were cut at various positions near the center portion of the test plate. An attempt was made to cut the specimens at unusual occurrences indicated on the recording tapes or by observation. The specimens were measured by the gage shown in Fig. 7 as well as by direct measurement on the etched cross section.

OBSERVATIONS

The following conditions were observed:

1. The characteristics of the recorded current indication can be used to indicate the shape of the weld bead and to some degree the extent of weld penetration. Figure 8 illustrates three current records with a corresponding cross-section and plan view of the weld deposit.

Figure 8*a*, a thin curve with a smooth contour, indicates good normal conditions with smooth bead shape and good penetration. Minor general changes in curve contour do not appear to make noticeable deposit changes. Appreciable or erratic changes generally indicate a change in arc and flux pool conditions and also in deposit and penetration.

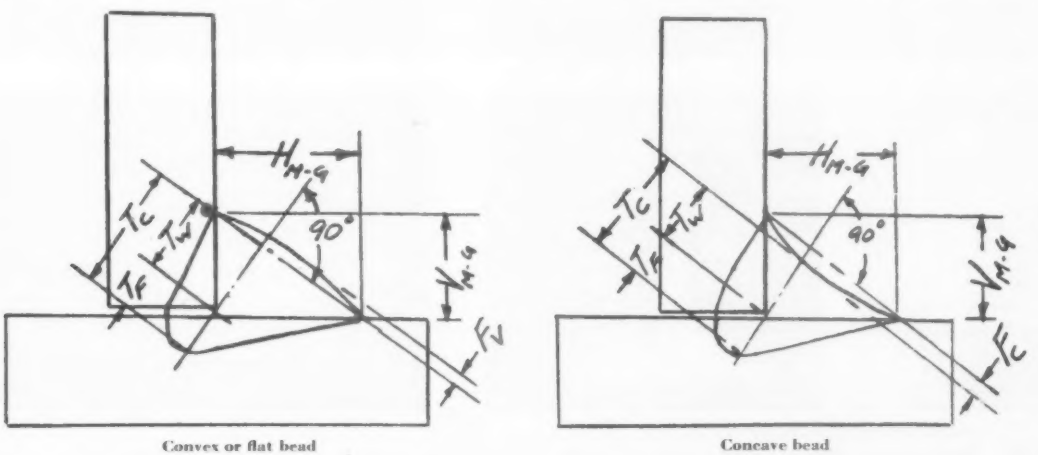


Fig. 6 Weld dimensions used for records

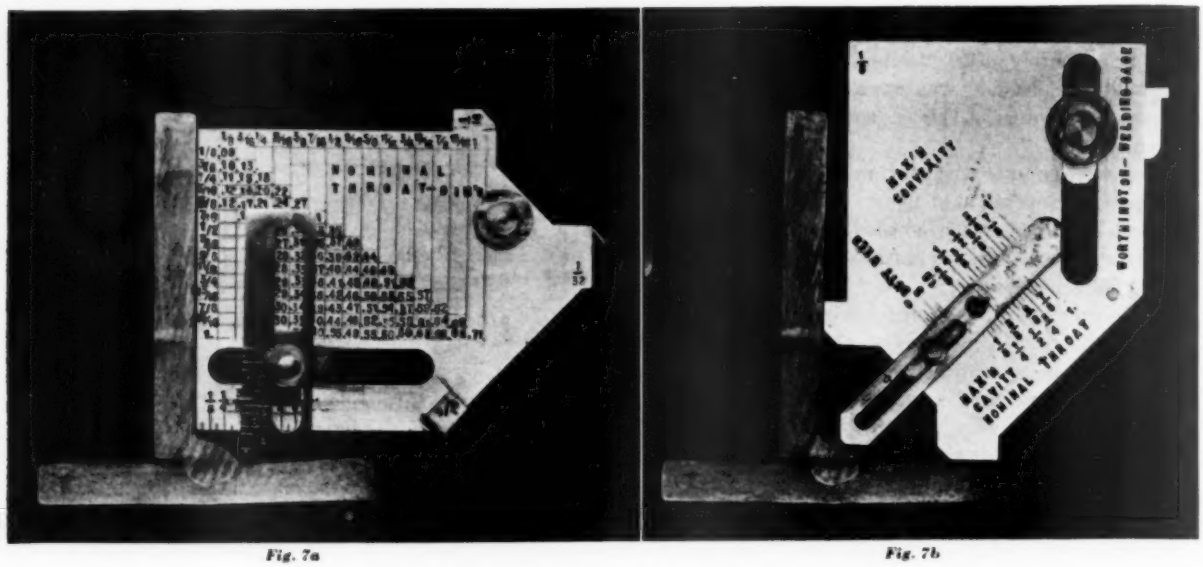


Fig. 7a

Fig. 7b

Fig. 7a, b Welding gage, front and back

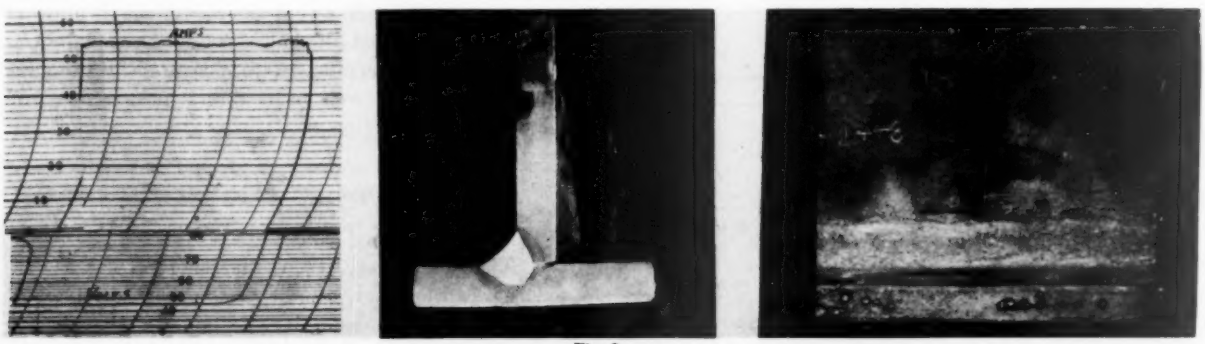


Fig. 8a

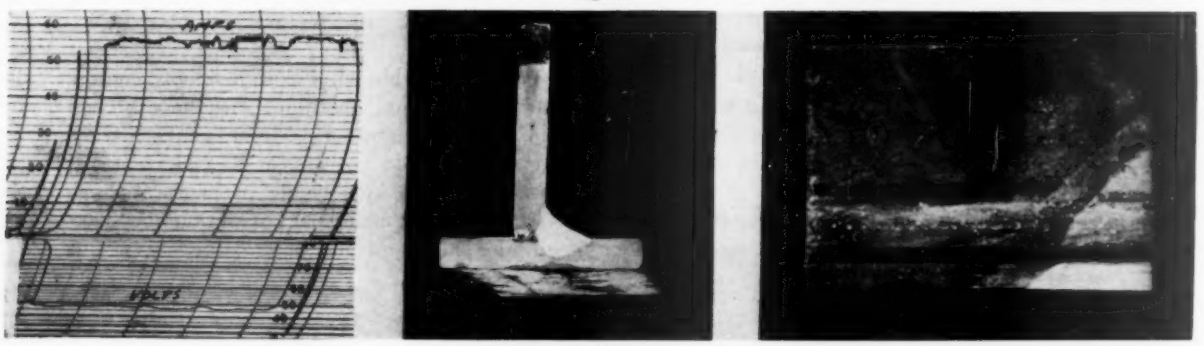


Fig. 8b

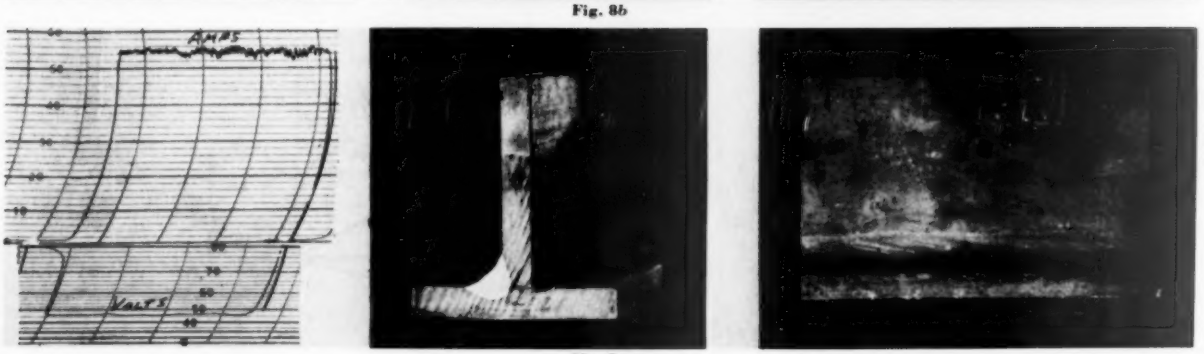


Fig. 8c

Fig. 8a Root $^{1}/_{32}$ in.; Hm = $^{7}/_{16}$ in.; Vm = $^{11}/_{32}$ in.; Tc = $^{31}/_{64}$ in.; Tw = $^{1}/_{4}$ in.; Tf = $^{15}/_{64}$ in.
Fig. 8b Root $^{3}/_{32}$ in.; Hm = $^{17}/_{32}$-in.; Vm = $^{13}/_{32}$ in.; Tc = $^{5}/_{16}$ in.; Tw = $^{1}/_{4}$ in.; Tf = $^{1}/_{16}$ in.
Fig. 8c Root $^{1}/_{32}$ in.; Hm = $^{7}/_{16}$ in.; Vm = $^{11}/_{32}$ in.; Tc = $^{11}/_{32}$ in.; Tw = $^{1}/_{4}$ in.; Tf = $^{3}/_{32}$ in.

Figure 8*b* illustrates a curve with a ragged contour and indicates decreased penetration and an erratic arc pool condition such as obtained by improper weld position or improper flux pool support, erratic or excessively poor joint fit-up for current being used.

Figure 8*c*, a bushy curve caused by the weighted high-frequency current variations, generally indicates a ropey bead or a high convex bead shape with an appreciable decrease in penetration. This occurs frequently if the molten flux pool runs ahead of the arc and can also be caused by excessively low arc voltage. In cases of large single-pass weld size deposits where a large arc pool is maintained, this type of curve can result.

This type of curve is an indication of a general decrease in weld penetration over that obtained in Fig. 8*a* as well as a possible change in weld deposit profile. In case of excessive root opening, a curve of this nature can result in deep penetration *Tf*, but a poor bead shape.

2. It was observed that an optimum voltage existed for a definite current range as illustrated in Fig. 9. Generally this voltage was also the minimum that could be used with assurance of a desirable weld bead profile.

3. From an over-all observation it was found that a tolerance of plus $^{3}/_{32}$ in. and minus $^{1}/_{32}$ in. on any single pass fillet weld from $^{3}/_{16}$ in. through $^{3}/_{8}$ in. size, inclusive, was not exceeded, and on $^{7}/_{16}$ in. size the tolerance was plus $^{3}/_{32}$ in. and minus $^{1}/_{16}$ in. On the $^{1}/_{2}$ in. size the tolerance was somewhat less (plus $^{1}/_{16}$ in. minus $^{1}/_{32}$ in.) and perhaps because only the flat position was used. These values are shown on the upper right side of Fig. 2 which also indicates that the excessive volume required by these tolerances is from 6 to 26% over that required for an equivalent normal equilateral fillet. This presents considerable improvement when compared to studies taken on jobs accomplished without control where the weld volume varied from minus 31% to plus 47% from that of the normal weld bead volume. In the latter respect the control being suggested assures more uniform and, consequently, greater average joint strength and lower peak weld stresses.

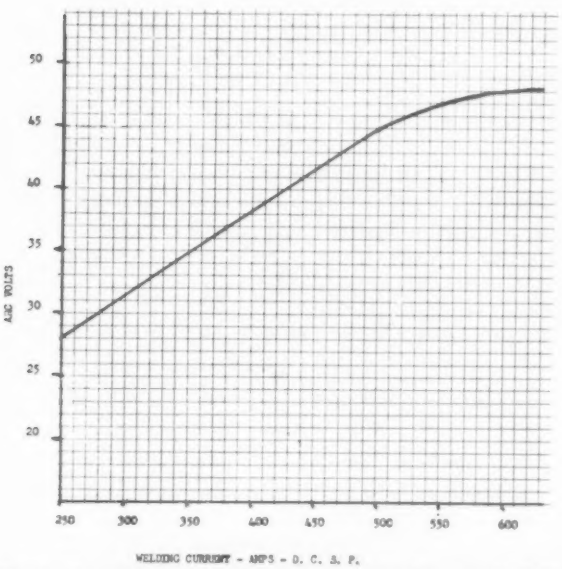


Fig. 9 Curve showing relationship of arc voltage and DCSP welding current

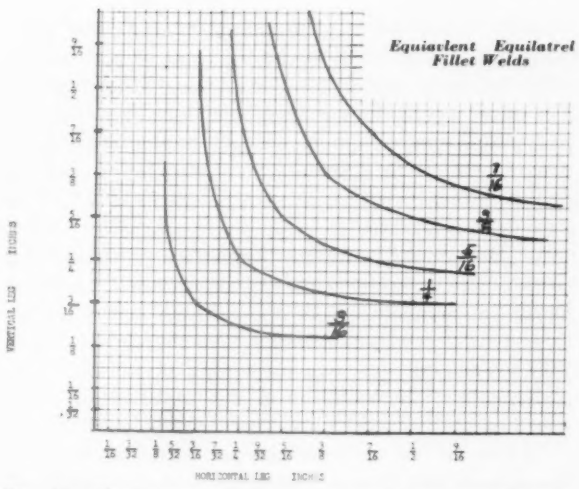


Fig. 10 Chart used to determine equivalent equilateral fillet deposit

4. The new weld gage developed during this study provided improved reading accuracy, decreased weld size reading time, and also provided visual reading of the minimum throat depth of unequal leg size fillet welds. The front and back views of this gage are shown in Fig. 7. This gage was found very effective and is described in more detail later.

5. It was observed that equilateral fillet welds were generally the exception rather than a rule and, consequently, the diagram shown in Fig. 10 was developed to assist in determining the equivalent equilateral weld deposits. By making use of these curves as an expedient to determine actual weld strength, the weld deposit arc time could be reduced up to 50% for equal joint strength. The equivalent equilateral weld size is obtained by reading the intersection of the horizontal and

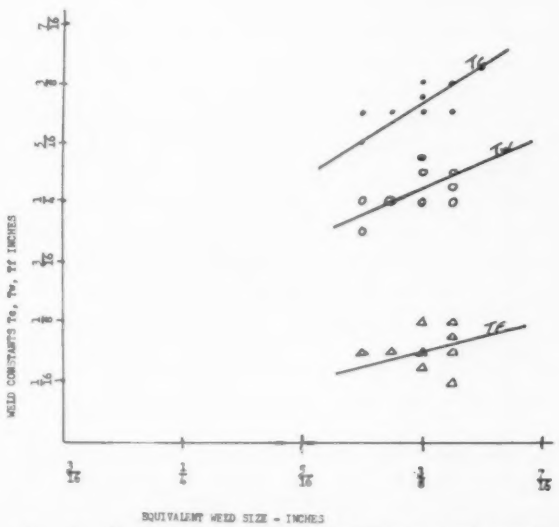


Fig. 11 Variation of weld constants in a single weld

vertical weld leg sizes. The equivalent equilateral weld size is the size which produces the same weld throat in either an equilateral deposit or an unequal leg deposit. The latter terminology is being introduced because current practice in the industry by shops and engineering departments is to designate weld size by equilateral deposit which produces a weld throat of 0.707 times weld deposit leg size.

6. Due to the prevalent occurrence of unequal leg sizes, it was determined to test the extent of variation along the complete length of weld and, consequently, specimens of good weld contour were checked for variation by cutting the complete length of weld at 1-in. increments. The variation of the weld constants *Tc*, *Tw*, *Tf* and the leg sizes are shown in Fig. 11.

The variations are quite similar to those occurring throughout this study. However, it can be observed that the size of weld and weld penetration are within those values being recommended for consideration of standards and, further, this curve illustrates to what extent the spread of reading points can be expected on test work done under shop conditions. The latter acted as a guide for interpolation of data obtained from the test studies.

7. It was found that the fusion area at the root of the joint was rather definite with only a few exceptions. Further, it was found that the total weld throat depth was considerably in excess of the value of 0.707 times the smallest leg size. Figure 12 is a composite study covering the total range of currents, weld positions and joint fit-up and covers the readings from 134 weld specimens and illustrates that the value of *Tc* (throat depth) was greater than 0.707 times the smallest leg size in all cases. Further, the throat depth of the weld deposit *Tw* was also generally greater than the 0.707 value. In Fig. 12 the lines marked 1.0, 0.8 and 0.707 are throat to leg ratios and are shown to indicate their relative positions to the measured weld deposit constants.

An analysis of the study indicated that in 83$^1/_2$% of all the welds measured, the value of *Tc* was equal or greater than the equivalent weld leg size and, further, in 95$^1/_2$% of the tests this value was equal to or greater than 0.8 of the equivalent leg size. Consequently, this should be considered as strong evidence that the fusion zone could be considered as part of the equivalent throat. The value of *Tf* was found to be over 20% of the value of *Tw* and, therefore, it appears that this value should be added to *Tw* when considering the weld throat *Tc* in stress calculation. The latter will allow reducing the specified weld size currently referred to in this report as *Tw*.

8. Penetration was found proportional to current provided the voltage is in accordance to the curve in Fig. 9 and, further, that the joint fit-up does not vary too drastically as shown in Fig. 13. The latter is shown by the lower line labeled *Tf* on Fig. 13 and is a composite for several flat welding conditions and also for zero to $^1/_{32}$-in. joint fit-up. This chart illustrates the general spread of results obtained during this test work and is shown to give a clearer picture of the type of data collected in these studies. The variations are to some extent caused by irregularities in joint fit-up caused by sheared and torch cut edges as all of this test work was made to simulate shop conditions as much as possible. Other conditions responsible for irregularities are a slight variation in welding arc speed and also a variation in flux blanket as well as flux can manipulation and arc current conditions. However, the variations are quite similar to those in Fig. 11 for one weld and, consequently, are an average working yardstick.

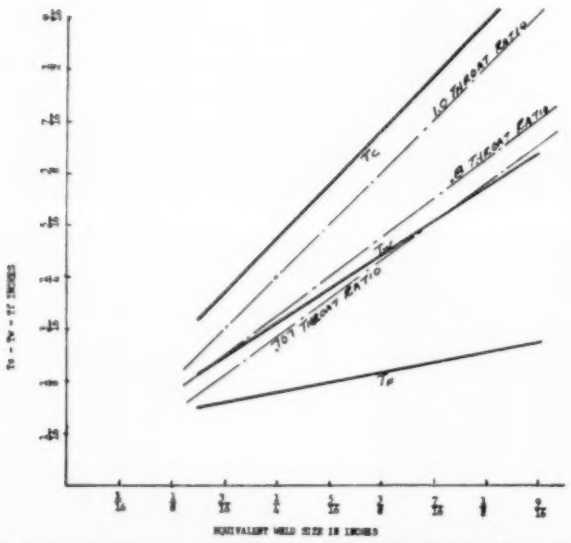


Fig. 12 Weld constants Tc, Tw, Tf plotted for all positions, currents and fit-up used in the studies

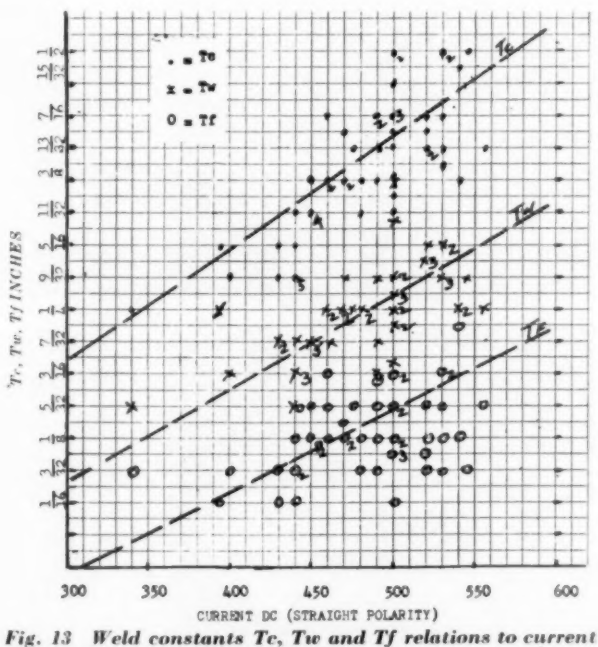


Fig. 13 Weld constants Tc, Tw and Tf relations to current for the joints on tight fit-up $^1/_{32}$-in. root opening (semi-automatic submerged process)

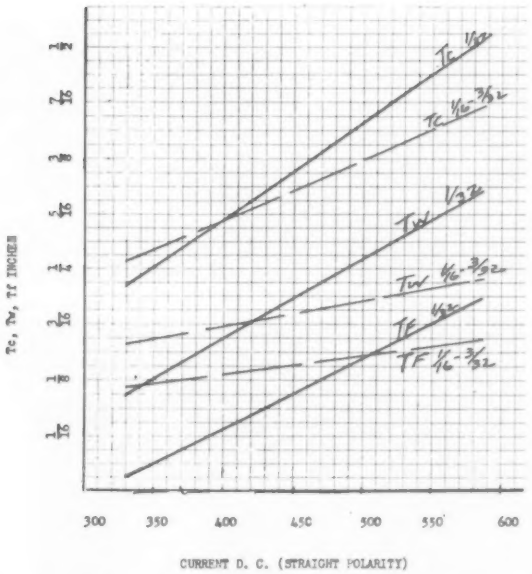


Fig. 14 Weld constants Tc, Tw, Tf relation to current for various joint fit-up openings

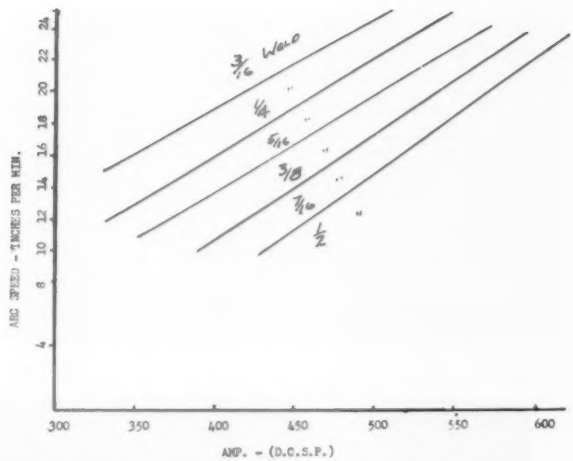


Fig. 15 Weld size (Tw) determining by arc speed and current for the horizontal and flat positions. Joint fit-up tight to $^1/_{16}$-in. open horizontal position not used over $^3/_8$-in. Tw.

Figure 14 illustrates the same constants as in the previous Fig. 13. However, it is a composite to illustrate what changes take place as the joint fit-up opening is increased from $^1/_{32}$ in. max to $^1/_{16}$ and $^3/_{32}$ in., inclusive. It can be noted that when changing the fit-up, the total weld throat *Tc* does not change drastically on amperes up to approximately 500. However, the weld deposit throat *Tw* and the fusion depth *Tf* do change to a considerably greater degree, but *Tf* increases with poor fit-up to offset the decrease in the weld deposit build-up *Tw* and thereby maintains approximate uniform total throat depth *Tc*.

Inspection of Figs. 13 and 14 will provide additional evidence that the throat depth through the fusion area *Tf* is at least 20% of the throat area *Tw* in the weld build-up and, consequently, should be used in determination of weld strength and stress calculation.

9. The weld deposit and throat depth *Tw* are proportional to the arc speed for a constant current with an optimum voltage as indicated in Fig. 9. This is illustrated in Fig. 15 and represents a composite of the various flat and horizontal positions for root openings up to $^1/_{16}$ in., inclusive. The horizontal position was not used on weld sizes over $^3/_8$ in. It can be observed that an arc speed variation of approximately 2 to $2^1/_2$ in. is obtained between various weld sizes for a constant current and approximately 35 to 50 amp variation in current for a constant speed. Plate thickness had little effect in this study except the $^1/_4$-in. plate on current valves over 450 amp where there was excessive penetration to produce near blow-through conditions.

DISCUSSION

Figure 16 illustrates a group of specimens which were produced in a consecutive manner to determine the weld dimensions and also the consistency of weld dimensions when welding manually at a predetermined speed. These specimens were welded at arc speeds of 9, 18 and 27 ipm and at a constant current and the analysis indicates remarkable uniform bead shape and weld dimensions. It also indicates the reliability of obtaining root penetration in that with a change of over 300% in arc speed, the minimum fusion in the base material *Tf* is 33% of the equivalent leg size and the maximum value is 100% of this same constant. The shallower value of penetration *Tf* into the plate on the larger weld sizes is caused by the relatively larger and thicker arc and slag pool which interferes with the base material penetration.

The maximum variation in leg size on any group of welds in this study was from plus $^1/_{32}$ in. to minus $^1/_{64}$ in. from normal and confirms the data in Fig. 2. When setting up standard procedures it is important to select a minimum number of speeds when making various weld sizes by selecting proper current combinations. Welding operators can reproduce predetermined arc speeds with amazing accuracy after some experience.

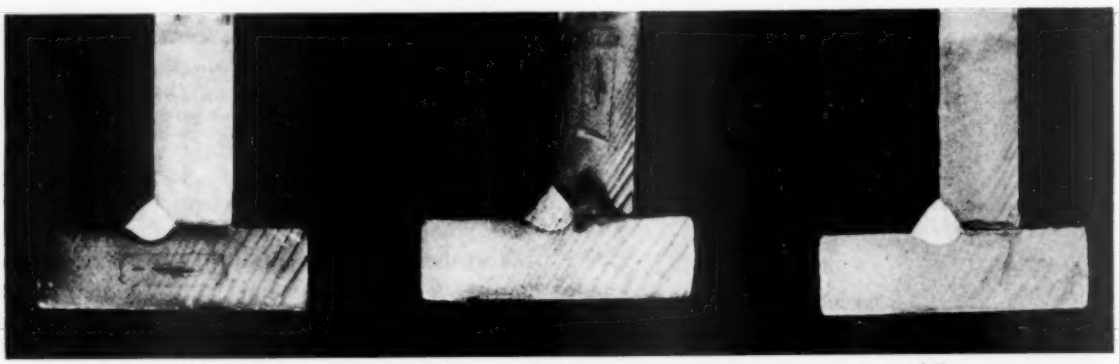
Figure 17 illustrates the changes in bead shape when welding in the flat position and at various lead angles such as level, downhill and uphill. Generally, welding uphill produces a convex bead profile while welding downhill produces a concave bead profile. The level position produces a flat bead but its flatness depends on the flux can position and current-voltage ratio. Further, the downhill position produces shallower weld penetration *Tf* which can be useful in welding over large joint fit-up openings. A finer grain flux and a heavier flux blanket generally produced a smoother bead profile.

Welding at various lead angles in the horizontal position also produces similar changes in weld bead profile. These conditions are identical in nature to those shown in Fig. 17 for the various lead angles. However,

in the horizontal position there is a greater tendency for undercut and overlap and, consequently, the weld profile is not quite as definite as in the flat position. The horizontal level and horizontal downhill positions are very advantageous from an arc blow-through condition when welding on poor fit-ups such as $^{3}/_{32}$ and $^{1}/_{8}$ in. A general observation made during these studies indicated that as the root opening increases the welding position should more definitely be horizontal downhill.

The over-all strength constants of the weld were not detrimentally affected by root opening. This was indicated in Fig. 14, but is more clearly shown in Fig. 18. In specimens where the weld pool ran through the joint and, consequently, the weld build-up *Tw* was decreased, the weld strength *Tc* was not too seriously affected. There is, of course, a question of suitable fatigue strength properties of the joint with the poor joint fit-up. However, in actual practice it is doubtful if the weld joint with poor fit-up shown in Fig. 18 would pass visual workmanship qualities and especially for fatigue application.

As stated previously, considerable search was made on the manipulation of the flux can. It was found that for the horizontal position a work angle of

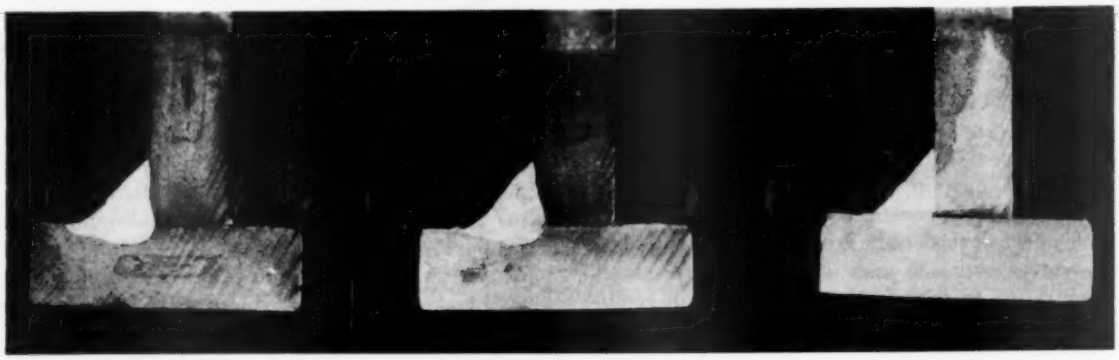


(a1) (a2) (a3)

(a) Flat 55-deg level, 27 ipm; 500 amp; 43 v. (a1) *Hm* 1/64; *Vm* 9/32; *Tc* 29/64; *Tw* 3/16; *Tf* 11/64; *Fc* 3/64. (a2) *Hm* 1/64; *Vm* 9/32; *Tc* 3/64; *Tw* 3/16; *Tf* 3/16; *Fv* 1/64. (a3) *Hm* 1/64; *Vm* 5/16; *Tc* 3/64; *Tw* 3/16; *Tf* 3/16; *Fv* 1/64.

(b1) (b2) (b3)

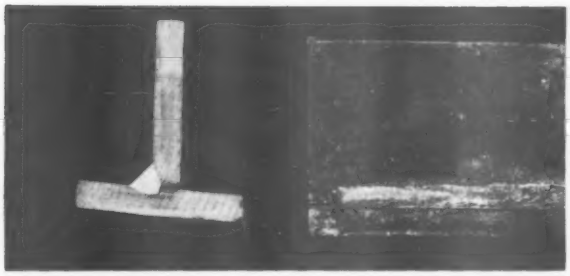
(b) Flat 55-deg level, 18 ipm; 500 amp; 43 v. (b1) *Hm* 3/64; *Vm* 11/32; *Tc* 29/64; *Tw* 17/64; *Tf* 3/16; *Fc* 3/64. (b2) *Hm* 13/32; *Vm* 9/64; *Tc* 7/16; *Tw* 1/64; *Tf* 3/16; *Fv* 3/64. (b3) *Hm* 13/32; *Vm* 3/64; *Tc* 7/16; *Tw* 1/64; *Tf* 3/16; *Fv* 3/64.



(c1) (c2) (c3)

(c) Flat 55-deg level, 9 ipm; 500 amp; 43 v. (c1) *Hm* 1/64; *Vm* 3/16; *Tc* 1/64; *Tw* 17/32; *Tf* 3/32; *Fc* 0; *Vc* 0. (c2) *Hm* 21/32; *Vm* 17/32; *Tc* 1/64; *Tw* 3/64; *Tf* 1/64; *Fv* 3/64. (c3) *Hm* 3/64; *Vm* 1/64; *Tc* 13/32; *Tw* 13/32; *Tf* 1/16; *Fv* 1/32.

Figure 16



(a)

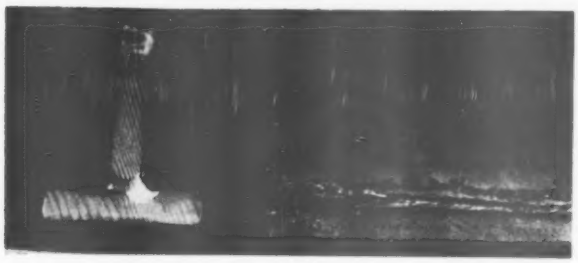
(b)

(c)

(a) *Hm* 3/32; *Vm* 3/64; *Tc* 13/32; *Tw* 9/32; *Tf* 1/16; *Fc* 0; *Fc* 0. Flat level 1/2 fit; 530 amp; 45 v. (b) *Hm* 13/32; *Vm* 7/16; *Tc* 7/16; *Tw* 1/64; *Tf* 5/16; *Fc* 1/16; Flat downhill 5-deg 1/2 fit; 540 amp; 45 v. (c) *Hm* 11/32; *Vm* 5/64; *Tc* 1/32; *Tw* 1/64; *Tf* 1/64; *Fc* 3/32. Flat uphill 1/2 fit; 530 amp; 46 v.

Figure 17

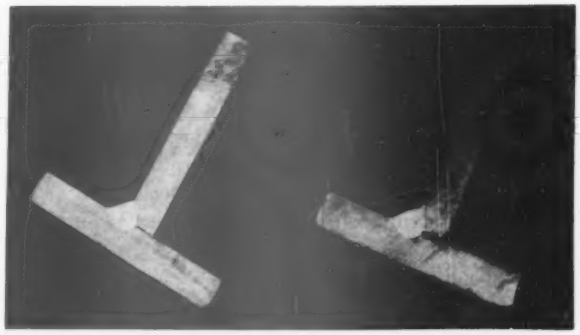
(a)



(b)

(a) *Hm* 1/64; *Vm* 13/32; *Tc* 9/32; *Tw* 7/32; *Tf* 1/16; *Fc* 0; *Fc* 0. Flat 55-deg level 1/2 fit; 430 amp; 43 v. (b) *Hm* 5/16; *Vm* 5/16; *Tc* 5/16; *Tw* 5/64; *Tf* 3/64; *Fc* 0; *Fc* 0. Flat 45-deg level 3/32 fit; 440 amp; 40 v

Figure 18



(a) (b)

(a) *Hm* 9/32; *Vm* 9/32; *Tc* 7/16; *Tw* 7/32; *Tf* 7/32; *Fv* 1/32. Flat 55-deg. level 1/16 fit; 550 amp; 43 v. (b) *Hm* 11/32; *Vm* 9/32; *Tc* 3/64; *Tw* 1/64; *Tf* 1/64; *Fv* 1/32. Flat 45-deg level 1/32 fit; 470 amp; 42 v.

Figure 19

approximately 45 deg was the most desirable. A greater angle, such as 60 deg, although desirable to minimize welding skill, required for weld on poor fit-up conditions, causes excessive flatness and excessive unequal leg conditions. A smaller angle, such as 30 deg., produces difficulty in obtaining smooth welds without undercutting on the vertical plate.

From preliminary test it was found that for the flat welding position the desirable angle is 55 deg instead of the usual 45 deg generally used. Figure 19 illustrates the change in penetration pattern. The latter is important when attempting to obtain complete penetration on the heavier plate such as 1/2 in.

The welding gage shown in Fig. 7 was developed to assist in quicker and more accurate reading of fillet welds. The gage can also be used to check weld fit-up (root opening) on fillet and butt welding as well as checking the reinforcement of the butt weld. Standard gages currently available require making two complete readings (one for each leg) on fillet welds and then a calculation or a reference check to another chart not on the gage. Further, the present gages require large overhangs on each plate at the joint to make the two leg readings and also to make accurate readings the two plates forming the joint must be at 90 deg.

The new gage developed allows making both leg measurements at one setting and then, by referring to the table on the gage, make the accurate determination of the equivalent equilateral weld size which is generally specified on the drawing. The construction of the vertical movable leg rule allows for considerable angle deviation from 90 deg without interferring with accurate measurement. A second operation at the other end of the gage allows measurement for excessive concave or convex bead profile.

Figure 7 illustrates the operation in weld measurement. The horizontal rule indicates a 7/16-in. leg and the vertical rule a 1/4-in. leg and by referring to the chart at the intersection of these two readings, the minimum throat dimension is 0.22 in. Referring to the outer row of figures on this chart showing the dimensions for two equal leg intersections, it is found the closest equilateral weld deposit would be a 5/16 in. weld

size and, therefore, the unequal leg deposit is equivalent to a $^5/_{16}$-in. specified weld.

To check whether the weld has the proper profile, the nose of the gage is set into the joint also shown in Fig. 7*b* and then the slide is moved until it contacts the weld. If the weld is of proper profile, the zero line on the rule will not read on the maximum convex scale over $^5/_{16}$ in. and on the maximum concave scale under $^5/_{16}$ in. size.

CONCLUSION

It appears very feasible to economically provide for simple but effective controls on the manually operated submerged-arc welding process. The control system would provide for use of a recording ammeter and voltmeter (or equal equipment) which could be set in the welding circuit at the operator's station. The amperages, voltages and visual inspection of the recording profile shape and length of recording would provide suitable controls which with the aid of the welding gage illustrated previously and visual quality inspection would provide safe determination of weld strength and adherence to Code quality.

It is the earnest desire of the authors that further work be accomplished by other production organizations and Code groups, such as the AWS to further this program. A substantial saving in manufacturing cost and conservation of raw materials and increased production output will be the ultimate result to the welding industry.

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(*Continued from page 448*)

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Austenitic Manganese Steel Welding Electrodes

Properties and welding techniques for use of molybdenum-manganese electrodes in repair and hard facing of manganese steel parts and for miscellaneous hard-facing applications

by Howard S. Avery and Henry J. Chapin

Abstract

Electrodes of work-hardenable austenitic manganese steel are used for assembly, repair and hard facing of manganese steel parts as well as for miscellaneous hard-facing applications.

Nickel-manganese electrodes have been most popular in the past, but they are now being supplanted with molybdenum-manganese grades that are equivalent in most respects and superior in yield strength and flow resistance.

Detailed properties for these materials are described, their relation to specifications shown and instructions for welding provided. Structural changes that may occur from welding are illustrated and precautions to avoid welding damage are indicated.

As overlays these materials provide good wear resistance for many applications. They may be applied by manual and automatic arc welding. The electrodes may be provided with flux coatings, but the compositions are unique in their ability to provide good welds when used bare. This is especially advantageous in automatic welding since it avoids the expense and trouble of the flux blanket that is needed for submerged arc welding.

INTRODUCTION

THE unique properties of austenitic 13% manganese steel are fairly well known in the engineering field, since Hadfield's first patent* was one of the earliest issued to cover modern alloy steels. Its toughness, its capacity to work harden, its nonmagnetic behavior and its resistance to some kinds of wear have earned manganese steel an enviable reputation wherever punished parts represent an industrial problem.

Castings account for most of the industrial tonnage, but simple wrought shapes like plates and bars are also widely used. Rails formerly were an important wrought product, but at present manganese steel appears in trackwork chiefly as cast frogs and the inserts at the points of maximum wear in crossings.

The welding of such a useful alloy naturally has received considerable attention. It was soon recognized that it was unusually sensitive to reheating and could embrittle from this effect to the point of losing its characteristic toughness. Oxy-acetylene welding is so likely to produce embrittlement that it is not accepted as a practical method for this alloy. Properly handled electric arc welding has been successful, and thus the electrodes and correct techniques for using them are matters of considerable interest and importance.

Most of the electrodes currently employed are modifications of the basic composition used for castings and wrought products. This paper will review some of the important properties that affect their performance in service, and will relate them to welding behavior and the properties of weld deposits.

COMPOSITION

The ASTM Specification for Designation A128–33 brackets the range from 1.0–1.4% carbon and 10–14% manganese. The nominal composition for castings is 1.2% carbon and 13% manganese. Without the manganese it would be a high carbon tool steel. As such, heating well into the red heat range would produce austenite, the nonmagnetic form of iron that can dissolve considerable carbon. The austenite is tough, relatively soft and is not usually stable at ordinary temperatures. If slowly cooled the dissolved carbon will separate out near 1000° F as iron carbide in the lamellar structure known as pearlite. If rapidly cooled, as by water quenching, the austenite of tool steel will avoid pearlite formation, but near atmospheric temperature it will suddenly change to the very hard structure called martensite. Adding manganese slows down the formation of pearlite and depresses the martensite formation temperature. With above 10% manganese these effects are so potent that transformation is prevented entirely by a water quench, and the tough austenite is retained at atmospheric temperature. This is the essential contribution of the manganese.

Other elements also stabilize austenite. The combinations of chromium and nickel in the stainless steels provide a familiar example; in fact several of the properties of manganese steel can be obtained with austenitic stainless steels. Manganese is less expensive, however. Carbon has an effect, especially in the presence of other alloys.

Silicon is usually present in manganese steel as a deoxidizer. It also raises the yield strength and can be used up to about 2%. At 2.5% it causes marked embrittlement.[1] Sulfur is controlled by manganese and is seldom reported above 0.025%. It readily forms innocuous globules of manganese sulfide that can be observed in the microstructure if they have not floated out in the slag. Phosphorus is a common impurity. It adversely affects room temperature tensile properties above 0.10%[1]. Current mill specifications include a phosphorus maximum of 0.07% for wrought welding electrodes.

Molybdenum and nickel are sometimes added to castings and wrought forms of manganese steel, but one or the other is usually present in electrodes. Their effects will be discussed under tensile properties. Chromium is generally present as a residual element, but several percent may be intentionally added. Like carbon, it tends to increase yield strength[1] and lower ductility. One approximation of its effect is to consider 1% chromium as the equivalent of 0.1% carbon.

Most of the commercially available electrodes are of the molybdenum-manganese or nickel-manganese types. These have been recognized by the AWS-ASTM Subcommittee on Surfacing Filler Metals and tentative specifications for them are in progress. The specified composition ranges are:

	C %	*Mn* %	*Si* %	*Ni* %	*Mo* %	*P* %
E FeMn (a)	0.70 to 0.90	13 to 16	0.4 to 1.3	3.5 to 5.0	—	0.07 max.
E FeMn (b)	0.70 to 0.90	13 to 16	0.4 to 1.3	—	0.6 to 1.4	0.07 max.

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* U. S. Pat. 303,150 issued Aug. 5, 1884.

Types have appeared on the market that do not conform to these ranges. When they contain high manganese but only insignificant amounts of other alloying elements, they are usually recommended only for build up of worn areas. Their toughness is inherently lower than that of the molybdenum-manganese or nickel-manganese grades that are preferred for critical applications.

Other grades have incorporated alloying elements with low manganese levels. They have not been generally accepted as equivalent to the high manganese types covered by this paper. Occasional apparently successful applications should be accepted with reservations since in these cases the full toughness of austenitic manganese steel probably was not required.

The austenitic chromium-nickel stainless steels have a different status. Some of them approach the toughness of manganese steel and can be interchanged in many welding situations. Their salient disadvantages are their cost and their resistance to oxy-acetylene flame cutting. Lower wear resistance is also sometimes a liability. They represent an area of experience in welding manganese steel because of large war surplus stocks that sold at considerably below their real value. Under normal market conditions their high cost justifies their use only for special purposes, such as the provision of a buffer layer between manganese steels and carbon steel, as described in page 478. These stainless steels (and modifications of them that contain unusually high manganese) are outside the scope of this paper.

CONSTITUTION AND STRUCTURE

The chief constituent of manganese steel is austenite, the nonmagnetic form of iron that can hold considerable carbon in solid solution. Austenite that is nearly saturated with carbon is responsible for the unique properties of this steel.

The austenite is not entirely stable. It will reject some of the carbon at intermediate temperatures or during deformation. This, in the form of manganese-iron carbides, occurs as fine particles, as films at grain boundaries, as flat, brittle plates, and in the lamellae of pearlite. Carbide precipitation in any of these forms leads to increased hardness and brittleness. Deformation (work hardening from pounding, etc.) raises hardness most effectively with the least loss in toughness. Carbide precipitation caused by slow cooling from the completely austenitic range or by reheating the tough structure is undesirable.

The normal tough structure is produced in manufacture by water quenching from above 1800° F. Weld deposits depend on modified compositions to approach this toughness after air cooling from the welding heat.

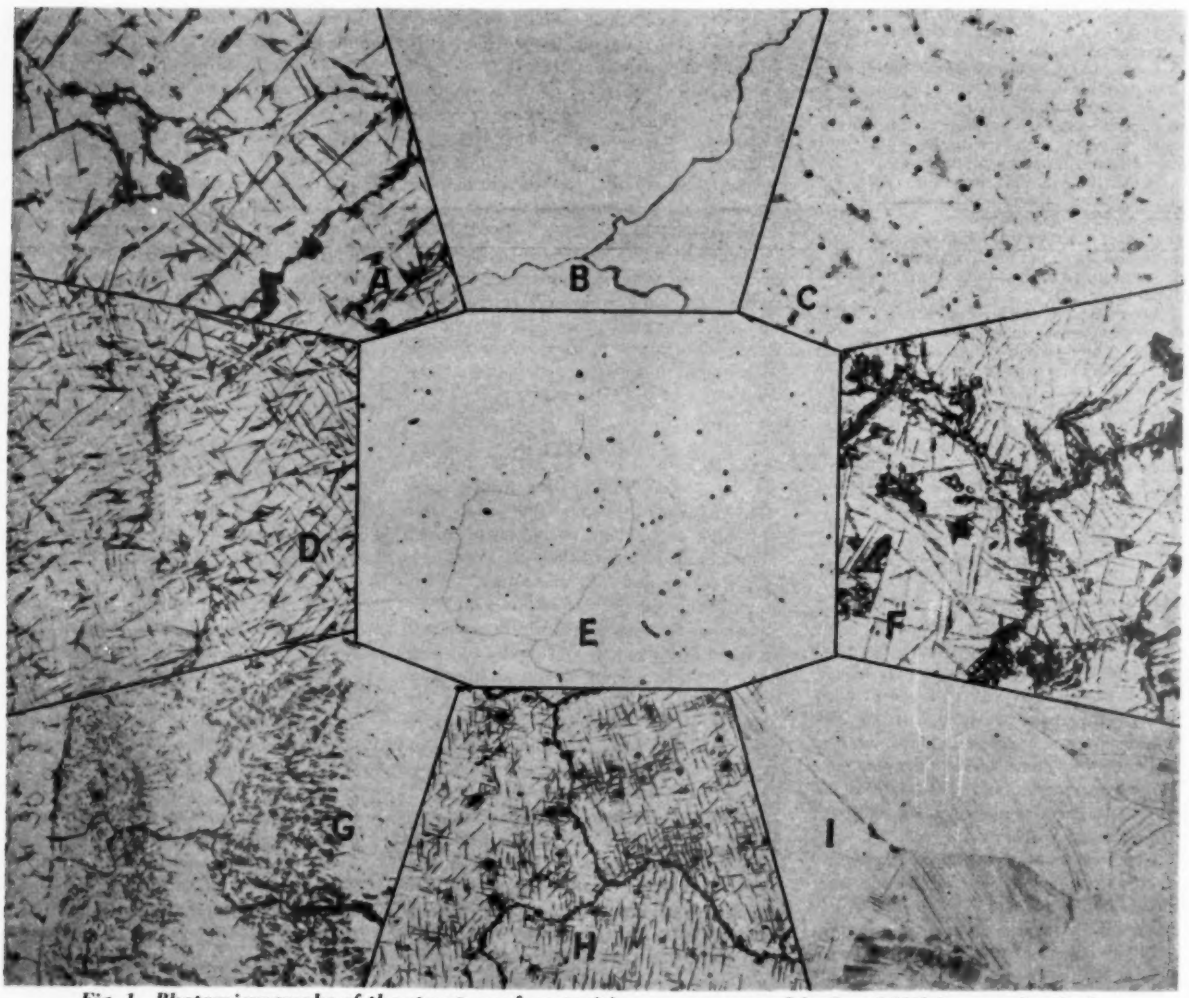

Fig. 1 Photomicrographs of the structure of austenitic manganese steel (reduced 20% in reproduction)

(*A*) C23301: as-cast; austenitic manganese steel; structure is brittle; 100 ×. (*B*) M75101: imperfectly heat-treated manganese steel (section was too heavy, allowing slow cooling during a water quench); 500 ×. (*C*) N12501: imperfect heat treatment (temperature of 1600° F was too low even though water quenching was satisfactory); 100 ×. (*D*) M12901: manganese steel reheated 48 hr at 700° F; 250 ×. (*E*) M7801: standard cast austenitic manganese properly heat treated; 100 ×. (*F*) A36302: manganese steel reheated 2 hr at 1000° F; 250 ×. (*G*) T252001: arc weld of nickel-manganese steel (right) on mild steel (left) showing the brittle structures that develop in the intermediate fusion zone; 250 ×. (*H*) P69401: reheated 6 min at 1100° F (this can occur during welding); 100 ×. (*I*) J136213: cold-worked austenitic steel; 100 ×.

Figure 1 shows a number of photomicrographs of manganese steel structures. The center picture (Fig. 1*E*) is clear austenite with fine grain boundaries outlining the individual crystals. The thickness of the boundaries is related to toughness. Sometimes with optimum heat treatment they are scarcely detectable. In contrast, slow cooling from an imperfect quench or from air cooling will result in thick boundaries and lower toughness, as in Fig. 1*B*.

Cast manganese steel will frequently show rounded inclusions and microscopic shrinkage voids, as in Figs. 1*C* and 1*E*. In the amounts shown here they are considered innocuous.

Because of the wide possible range in cooling rates, cast structures exhibit great variety. The one in Fig. 1*A* from a small casting has considerable carbide in the form of flat plates like the reheated structure of Figs. 1*D* and 1*F*. Heavy castings may have much of the precipitated carbide at grain boundaries. The purpose of the conventional heat treatment is to convert the structure like Fig. 1*A* to that of Fig. 1*E* by heating and quenching. If the top temperature is too low, carbide solution will be imperfect, as in Fig. 1*C*, while a slow quench permits too much to precipitate at grain boundaries during cooling as in Fig. 1*B*.

The tough structure of Fig. 1*E* is readily embrittled by reheating because of carbide precipitation as in Figs. 1*D*, 1*F* and 1*H*. Near 1000° F, pearlite (the dark areas in Fig. 1*F*) is prominent as a transformation product. Note that the reheating condition of Fig. 1*H* (6 min at 1100° F) could readily occur during welding.

The carbide precipitation that occurs during cold deformation (work hardening) produces the "slip lines" seen in Fig. 1*I* and produces much less embrittlement than reheating.

Figure 1*G* shows the results of welding with a nickel-manganese electrode on mild SAE1020 steel (on the left edge). The austenitic weld metal (extreme right) is similar to Fig. 1*E*, but the intermediate fusion zone, which has an "average" composition between the two, develops considerable martensite or other acicular structures during cooling. It is brittle and prone to crack. This sort of structure should be carefully avoided.

PATENTS

One of the earliest welding rod patents for manganese steel was issued to Churchward in 1920 (U. S. Pat. 1,337,543). This covered 1.0–1.25% carbon and 3.0–13.0% manganese with an iron base. The top of this range will be recognized as Hadfield's manganese steel, but the lower limits are hardenable tool steels with quite different properties. The high carbon level and the general lack of understanding of this alloy system probably prevented it from becoming generally useful.

The first important patent was issued to F. A. Fahrenwald in 1931 (U. S. Pat. 1,815,464) and assigned to The American Manganese Steel Co. The claims bracketed the ranges of 0.5–2.0% carbon, 5–25% manganese, 25–90% iron and 0.5–50% of nonferrous iron group (nickel or cobalt) metal. The preferred range claim covers 0.5–1.5% carbon, 10–15% manganese, 1–10% nickel and 60–80% iron. Current nickel-manganese electrodes fall within this range. However, experience has demonstrated the desirability of avoiding the higher carbon levels. The patent lists a silicon range from 0.35 to 2.00% but does not include it in the claims.

In 1932 U. S. Pat. 1,876,738 was issued to B. H. Payne. His ranges of 0.60–0.85% carbon, 11.0–13.5% manganese, 0.60–0.95% silicon and 2.5–3.5% nickel fall entirely within the ranges shown by Fahrenwald. He makes a point that carbon and nickel are related and that less nickel is needed when the carbon is low. He claimed virtues of silicon above 0.60% as a fluxing ingredient in welding rod. These ideas are not entirely valid in relation to the slowly cooled (not deliberately heat-treated) steels, since nickel will not eliminate the tendency for high carbon to produce embrittlement. Moreover, fairly good welding electrodes can be produced without any nickel. Technically the greatest value of this patent is definition of a desirable carbon range for welding electrodes and for parts that are not to be heat treated. Even today acceptable nickel-manganese electrodes are produced with silicon below 0.60%.

On Dec. 31, 1935, two patents were issued to J. H. Hall. The first describes a method of producing "sound welds" for resurfacing or repairing manganese steel by depositing nickel-manganese of lower carbon content than the base metal (U. S. Pat. 2,026,467). The second defines the recommended welding rod, with the composition range 0.5–2.5% silicon. Claim 2 features up to 2% silicon in a nickel containing manganese steel weld rod, stressing its deoxidizing power. The remaining four claims cover "an iron or steel article" with 0.3 to about 1.0% carbon, 10–15% manganese and 1–5% nickel. Claims 3, 4 and 5 include silicon in effective amounts up to 2.5%, while claim 6 defines the 1.25–1.60% silicon range. Claim 4 introduces from 1 to 8% chromium while claim 5 stipulates sufficient chrome to cause the metal to harden under cold work (U. S. Pat. 2,026,468).

As a result of the earlier patents the features described by Hall were already a part of the industrial practice at the time his patents were issued. His claims were the first to mention chromium for welding rods, but the assumption that it is necessary to make the steel harden under cold work is erroneous. Silicon was, of course well known as a deoxidizer, but present experimental evidence can show that sound welds are not assured by following these patents.

Technically, the most valid statement of Hall's is to describe the role of low carbon in the deposited metal, which is "to dilute the relatively high carbon content of the partially fused or transition zone of the parent metal and so reduce or prevent the liberation of carbides." Fahrenwald believed that the embrittlement of manganese steel was due to austenite transformation while Hall attributes it to carbide precipitation. Both mechanisms can operate, but carbide precipitation is most likely to cause trouble in weld deposits. These patents all state that the weld rod is applicable for both gas and arc welding. Their lack of test data or experimental evidence is noteworthy. None of them offer any help in avoiding embrittlement of the heat-affected zone under the weld deposit and the fusion zone.

The claims of the four nickel-manganese patents overlap so much that it is fortunate that they did not lead to litigation. The several companies involved entered into cross-licensing agreements, and by the time the patents had all expired (1953) their products were quite similar. This makes possible the writing of a single specification. The clearest picture of the behavior of manganese steel was provided by Bain and his co-workers in the U. S. Steel Research Laboratories.[3] Their paper defines clearly the relation of carbon content, carbon solubility and temperature and is more valuable than all of the patent disclosures.

In 1934 J. B. Austin received a patent on welding electrodes intended for use on manganese steel. He remarks on the tendency of high carbon to cause weld cracking and describes a composite electrode that includes ferromanganese, ferrosilicon and carbon in the coating. This is applied on a 0.15% carbon, 3.5% nickel core wire. The total electrode composition is from 1.4 to 1.9% carbon and 12 to 17% manganese and was said to provide a deposit with 0.8% carbon, 12.6% manganese and 3.2% nickel. Welding practice is described as a crescent weave with 120–140 amp d-c reversed polarity (electrode positive). Improved arc stability and lower welding currents were virtues claimed in comparison with bare arc welding. Most manganese steel welding was then, and even is today, done with bare rods. It is not clear whether this patent was effectively exploited commercially, but coated electrodes have slowly become more prevalent. The composite form that depends on the coating to carry some of the essential alloying elements is less expensive to produce but is more likely to result in segregation and nonuniform deposit composition.

Meanwhile considerable unpublished research[3] was clarifying the role of various alloying elements on the austenitic manganese steels. It became evident that perhaps the most important role of nickel was to maintain the tensile strength of manganese steel in which carbon was

lowered to avoid carbide precipitation embrittlement. Various other elements would also do this, but since the nickel-manganese type was well established and generally satisfactory, there was little economic incentive to substitute until a nickel shortage was caused by World War II.

In 1936 a patent was issued to P. E. Jerabek that claimed composite electrodes and weld metal deposits that were "able to achieve substantially maximum hardness by means of a minimum of cold work by the presence of" tungsten, molybdenum or vanadium. His claimed ranges (in percent) included: C, 0.50–1.20; Mn 7.0–15.0; Ni, 0.10–10.0; Mo, <2; W, <2; V, <2. He describes the prior art as depending on 11–15% manganese rods containing 1.0–1.5% carbon and upon water quenching the welds to maintain an austenitic structure, which is an erroneous description. Lower carbon electrodes were common practice at this time and water quenching was seldom used in the field. He states that lowering of carbon to 0.6% eliminated weld cracking but reduced the work-hardening capacity so much that welds failed in service before they could harden sufficiently. The modifications he claimed were said to overcome these difficulties. Molybdenum will not in fact prevent weld cracking, and while it can under some conditions accelerate work hardening, this is not a desirable result.

This patent contains an interesting list of the various ways in which alloy deposits can be made by means of the welding arc.

Jerabek received another patent (U.S. Pat. 2,266,762) in 1941, in which he claims substitution of copper for nickel (or cobalt) in the formulas of his 1936 patent. The broad claims cover the ranges (in percent): C, 0.50–1.20; Mn, 7–15; Si, 0.20–1.0; Ni + Co, 0–10.0; Mo, <2; W, <2; V,<1; Cu, 0.10–10.0, as deposits from arc welding.

Narrower and more practical claims define electrodes that yield deposits in the range (in percent) of: C, 0.85–0.92; Mn, 12.5–13.5; Si, 0.40–0.60; Cu, 0.90–1.25, and at least one of these: Mo, 0.35–0.75; W, 0.35–0.75; V, 0.10–0.75, or weld deposits with less than 0.20% nickel and cobalt and C, 0.60–0.80; Mn, 10–13; Si, 0.25–0.45; Cu, 0.90–1.25; and at least one of these: Mo, 0.35–0.75; W, 0.35–0.75; V, 0.10–0.75.

All of these claims are modified by the maximum hardness by minimum cold work clause. Two others define unqualified compositions, the most significant of which is weld metal which upon normal cooling is predominately austenitic and which comprises (in percent): C, 0.60–0.80; Mn, 10.00–13.00; Si, 0.25–0.45; Cu, 0.90–1.25; Mo, 0.45–0.65. This has been exploited commercially. It is not in the range that causes accelerated work hardening.

The various patents fail to deal with two important properties: yield strength and inherent toughness. While some mention weld cracking, they do not deal with it effectively, and engineering test data are conspicuous by their absence.

Both yield strength and toughness can be measured by the conventional tension test, which will also show the progress of work hardening for austenitic alloys (Fig. 2). Toughness is indicated by the area under the stress-strain curve and is related to some kinds of wear resistance. The tough manganese steels have a long deformation range between the yield and the point of ultimate failure. They afford high values of tensile strength and elongation. Work hardening is taking place during this deformation. A steep slope beyond the yield strength indicates rapid work hardening; that, is the maximum strength (and hardness) is being obtained with less deformation (or cold work). Such steels show poor ductility and poor resistance to grinding abrasion in laboratory tests. They are readily produced by lowering carbon and manganese too far or by excessive amounts of such ferrite promoting elements as silicon and molybdenum.

The general absence of tensile data can be partially explained by the difficulties of machining manganese steel specimens; they usually must be ground in special equipment. However, part of the scarcity may stem from the cracks and other defects that are common in manganese steel weld deposits. To obtain good data with facility it has been advisable to work with cast specimens when studying alloy effects and to study weld defects with other techniques.

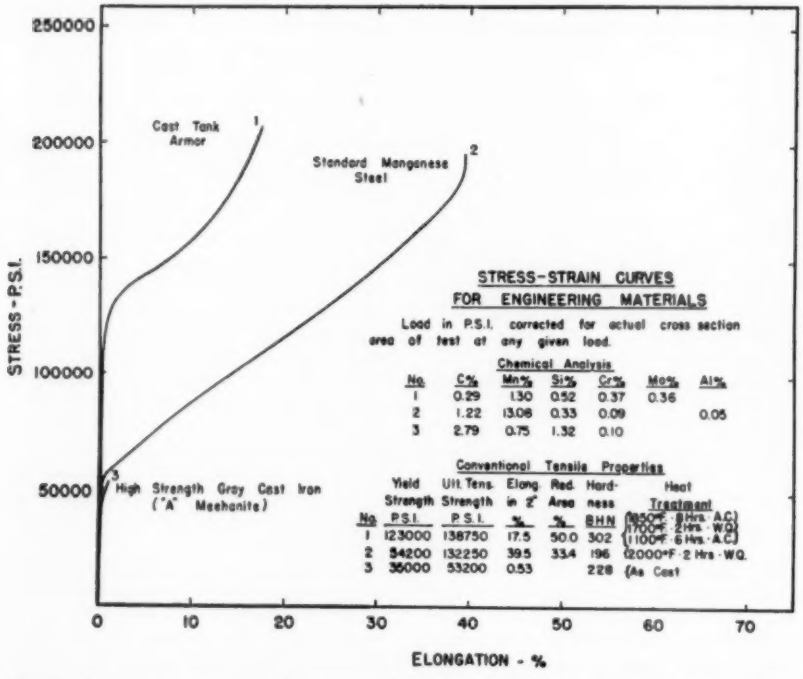

Fig. 2 Stress-strain curves demonstrating the toughness of austenitic manganese steel

INDUSTRIAL STATUS

The application of nickel-manganese electrodes after the late 1920's made possible fabrication and repairs of manganese steel that previously had been considered impracticable. This advance was generally attributed to the nickel content, whereas, in fact, it probably was due chiefly to lowered carbon. It is considered significant that welding of manganese steel in Europe, where the 1.2% carbon grade is widely used for electrodes, is generally less successful than in this country.

A substantial amount of low carbon manganese steel, either unalloyed or with insignificant amounts of molybdenum, copper, nickel, etc., has been employed for facing applications on manganese steel parts. This build-up use is a replacement of worn metal, usually subject chiefly to abrasion and compressive stresses. Results were apparently satisfactory, but for applications where tensile stresses were important or toughness was obviously desirable, as in railway trackwork, nickel-manganese steel electrodes were generally considered superior.

The war years of the 1940's required a change. Molybdenum was more available than nickel, and since the technical laboratory data were favorable to a substitution, the 1% molybdenum-manganese grade was introduced and accepted for general use. It performed well, but after the war there was a tendency to revert to the nickel grade because of the long experience

record with the latter. The pattern has recently reversed again because of the strategic character of nickel, which makes the data in this paper particularly pertinent. The evidence indicates that 1.0% molybdenum produces metal that is modestly superior to that with 3.5% nickel, at the same carbon level. This superiority is difficult for the user to detect except in applications where the higher yield strength is exploited, since most field tests of manganese steel welds are likely to be inconclusive or misleading unless they are carried out with the precision and control of laboratory experiments. Even in the laboratory a statistical approach, as described later, may be necessary to provide valid conclusions about weld behavior.

TENSILE PROPERTIES AND TOUGHNESS

Manganese parts are sometimes designed primarily for tension service, as in chains, but in most cases tensile stresses are only one of the hazards of a part selected to resist wear. Toughness is the quality most desired, which places a premium on ductility and strength together. One measure of toughness is the area under a stress-strain curve, as in Fig. 2. An approximation of this index that has been called the "merit number"[4] is obtained by multiplying the conventional ultimate tensile strength by elongation in the same test. Figure 3 is a bar graph of various alloys compared on this basis, with resistance to high stress grinding abrasion also shown. Note that the manganese steels provide the best combination of great toughness together with fair abrasion resistance.

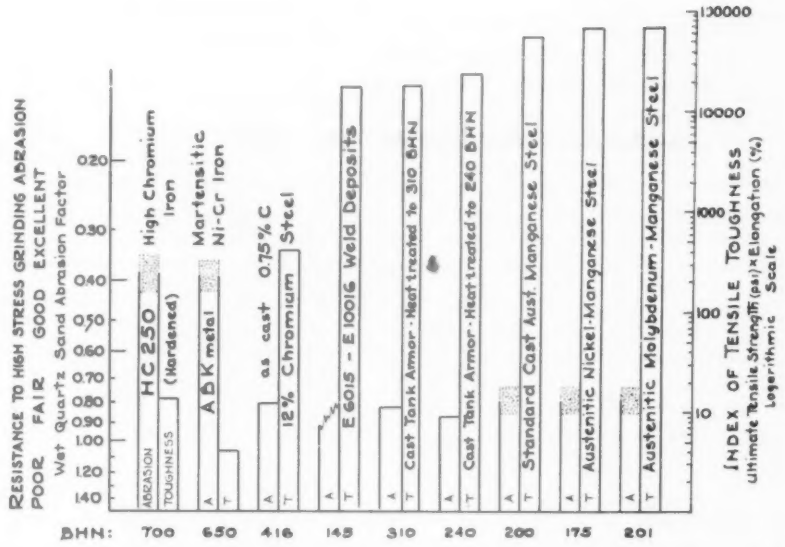

Fig. 3 Comparative tensile toughness and wet sand grinding abrasive wear of a group of abrasion resistance alloys

The average tensile properties of cast and toughened manganese steel appear in Table 1. It should be recognized that such an average of engineering properties is only the central tendency of a scatter band whose pattern is shown in Fig. 4.

Three series that show the effects of nickel, molybdenum and chromium on 1.15% carbon austenitic manganese steel appear in Fig. 5. From these it will be noted that nickel has a negligible effect on yield strength but that the other elements can raise it effectively. Carbon can raise the yield strength also[1] but like chromium it lowers ductility at the same time. From this group molybdenum emerges as the element able to increase the yield with the least impairment of elongation.

There is a hint that nickel can raise ductility. Experience has shown that this is pronounced at lower carbon levels. The average elongation of six 3.5% nickel, 0.78% carbon heats was 60% (but the low carbon dropped the yield strength to 46,300 psi). However, no commercial significance has been established for the difference between 40 and 60% elongation.

These properties are based on cast 1-in. diameter test bars, headed to ensure sound metal. Pouring temperature (and grain size) was not controlled as a variable in the group and thus the scatter band implied by Fig. 4 must be considered in the interpretation of the ultimate strength values in Fig. 5.

The separation of carbon along grain boundaries and crystallographic planes (as manganese-iron carbides) (Fig. 1) during the cooling of manganese steel austenite is responsible for loss in ductility.

Table 1—Properties of Standard Austenitic Manganese Steel

Form	Chemical composition, % C	Mn	Si	Tensile strength, psi Yield	Ultimate	Elongation, %	Reduction of area, %	Hardness, Bhn
As-cast, 1 in. diam	1.11	12.7	0.54	52,400	64,800	4		
				(specimen properties)				
Cast and heat treated, 1 in. diam	1.15	12.5	0.50	53,000	124,000	45		
				(average of 51 heats, including 106 specimens)				
Cast and heat treated, 1 in. diam expectancy	1.0	10	0.2	50,000	100,000	30	30	185
	1.4	14	1.0	57,000	145,000	65	40	210
Cast and heat treated in 4-in. section	1.11	12.7	0.54	52,000	89,600	25	35	
				(specimen properties)				
Cast and heat treated in 8-in. section	1.11	12.7	0.54	47,000	65,700	18	25	
				(specimen properties)				
Rolled and heat treated, 1 in. diam expectancy	1.1	11	0.2	50,000	131,000	40	35	170
	1.4	14	0.6	60,000	142,000	60	50	200

Elastic modulus: 28 million psi.
Work hardening capacity: From 170–200 Bhn to 450–550 Bhn.
Specific gravity: 7.9 Density: 0.286 lb/cu in. or 493 lb/cu ft.
Melting point: 2450° F. Specific heat: 0.12 g cal/g/° C.
Thermal conductivity: About 2.3% that of copper.
Electrical resistivity: 70 microhms/cm cube or about 7× that of pure iron.
Magnetic permeability: 1.003–1.03 (H = 24). Without surface preparation: 1.3±.
Wet quartz sand abrasion factor: 0.75–0.85 (vs. SAE1020 steel as 1.00) (high stress abrasion).
Dry quartz sand erosion factor: 0.41–0.56 (vs. SAE1020 steel as 1.00) (low stress abrasion).

Thermal expansion coefficients, in. per in. per ° F

100° F	200° F	300° F	400° F	500° F	600° F	700° F
0.0000102	0.0000106	0.0000111	0.0000119	0.0000126	0.0000134	0.0000136

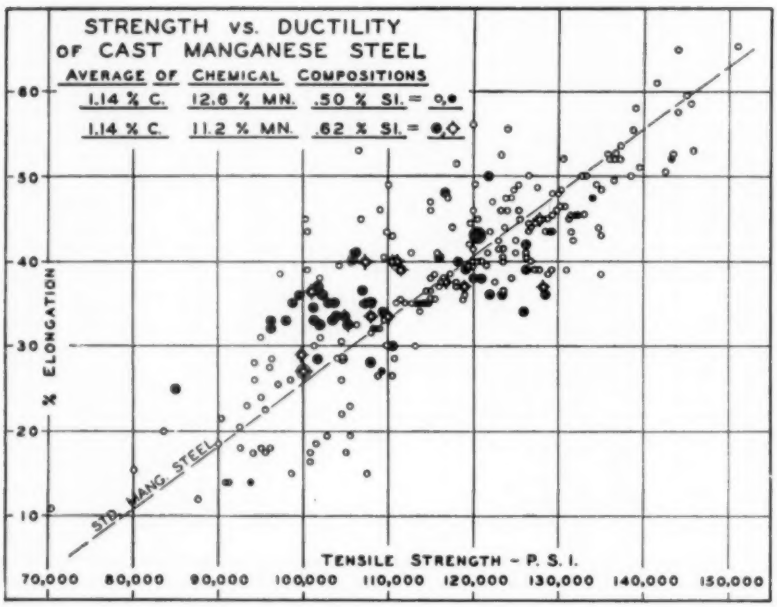
STRENGTH vs. DUCTILITY
OF CAST MANGANESE STEEL
AVERAGE OF CHEMICAL COMPOSITIONS
1.14 % C. 12.6 % MN. .50 % SI. = ○
1.14 % C. 11.2 % MN. .62 % SI. = ◇
% ELONGATION
STD. MANG. STEEL
TENSILE STRENGTH - P. S. I.
70,000 80,000 90,000 100,000 110,000 120,000 130,000 140,000 150,000

Fig. 4 Scatter band of austenitic manganese steel tensile properties

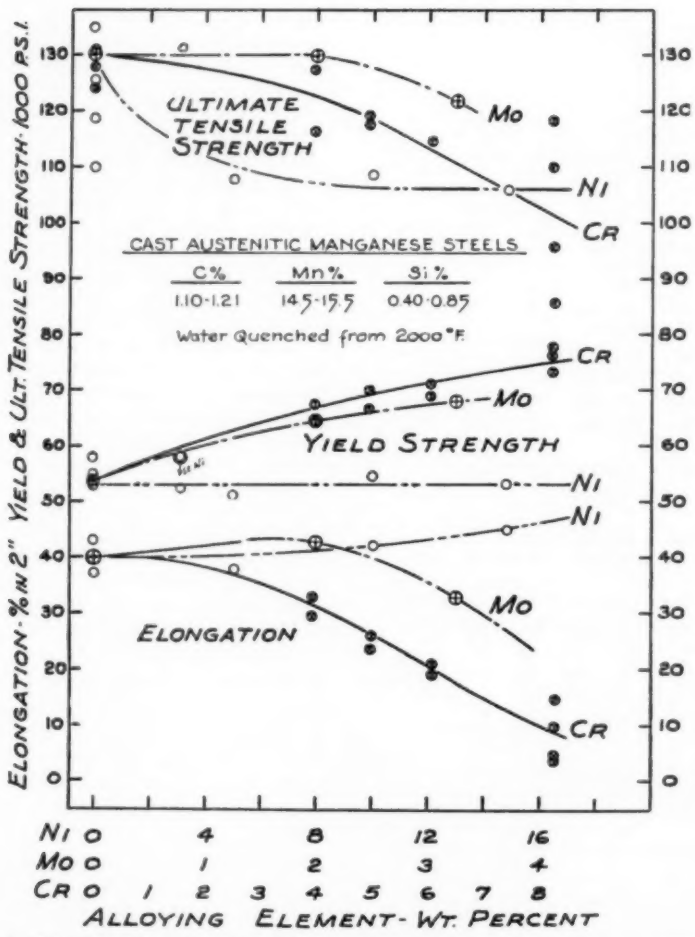
ELONGATION - % IN 2" YIELD & ULT. TENSILE STRENGTH - 1000 P.S.I.
ULTIMATE TENSILE STRENGTH
Mo
Ni
CR
CAST AUSTENITIC MANGANESE STEELS
C% Mn% Si%
1.10-1.21 14.5-15.5 0.40-0.85
Water Quenched from 2000°F
YIELD STRENGTH
ELONGATION
Ni 0 4 8 12 16
Mo 0 1 2 3 4
CR 0 1 2 3 4 5 6 7 8
ALLOYING ELEMENT - WT. PERCENT

Fig. 5 Effects of nickel, molybdenum and chromium on tensile properties of cast austenitic manganese steel, water quenched from 2000° F

The greater the amount of available carbon the more severe is the potential loss in toughness. The effect is prevented by the toughening heat treatment, which involves water quenching from a temperature high enough to dissolve the available carbon. However, it is usually inconvenient or impractical to heat treat manganese steel weldments. The relatively slow cooling from welding heat permits any marked tendency toward carbide precipitation to operate. The effect can be minimized by lowering carbon. This justifies carbon below 0.90% for welding, in comparison with the 1.10–1.25% carbon of the standard grade, providing other properties are not impaired.

The least complicated way to explore this carbon effect is by means of tensile tests on small castings. By cooling in the mold they simulate the rate of a slowly cooling weldment. The casting can be designed to provide sound test metal with a uniformity and reproducibility superior to weld deposits. It avoids the adverse effects of microscopic bead fissures, gas pockets, slag inclusions, incomplete fusion areas and such defects that are potentially present in weld deposits. It should be recognized that a heat-treated casting is not representative of weld metal. The casting must cool slowly, as does a weld deposit, for deductions involving their relationship to be approximately valid. Even in this case it is not implied that the tensile properties of welds or weldments are equivalent to those of slowly cooled castings. The latter may be considered as limiting values, to be matched only by perfect welds. Nevertheless, the trends established by studies of castings are considered to be valid for weld deposits of the same composition.

The effect of carbon on yield strength has been published.[1] In the range of our present interest it may be considered to raise the 0.2% set yield strength by about 285 psi per 0.01% carbon. Lowering carbon in the usual 12% manganese steel given the toughening heat treatment also lowers ultimate strength and elongation as indicated in Table 2.

Contrast this tendency for ductility to decrease with carbon content, after heat treatment, with the pattern of greater ductility at lower carbon after the slower cooling of the alloyed as-cast bars in Table 3. Notice also that the addition of 1% molybdenum makes possible a higher tensile strength at the 0.75% carbon level as well as good ductility.

It will be recognized that several compositions are involved in the response to welding. The electrode metal changes as described in Table 6 during deposition; the first layer of a deposit mixes with fused base metal to form a composition that depends on arc power and welding speed; subsequent layers involve different amounts of mingling with the base metal,

Table 2—Effect of Carbon Content on Heat-Treated 12% Manganese Steel

Mn: 11.5–12.5%; Si: 0.34–0.60%)
Water quenched from 1900° F

Heat No.	C %	Yield strength, psi	Ultimate tensile strength, psi	Elongation, %	Reduction of area, %	Hardness, Bhn
Avg.	1.12	53,000	124,000	45	35	195
49-304	0.73	46,000	109,250	33	28	163
49-210	0.61	42,000	96,750	21	18	167

Table 3—Tensile Properties of Cast Austenitic Manganese Steels After Slow Cooling and after Water Quenching

Heat No.	Chemical composition, % C	Mn	Si			Slowing cooling in sand molds: Ultimate tensile strength psi	Slowing cooling in sand molds: Elongation, % in 2 in.	Water quenched from above 1800° F: Ultimate tensile strength, psi	Water quenched from above 1800° F: Elongation, % in 2 in.
51-178	1.55	6.3	0.58			51,500	0	106,500	16
51-608	1.27	11.7	0.56		CG	52,000	2.0	107,500	39
					FG	69,500	2.5	134,000	42
45-033	1.11	12.7	0.54			64,800	4	104,800	39
48-053	0.95	13.0	0.51			61,250	14	109,500	46
51-607	0.85	11.2	0.57		CG	64,000	14.5	81,000	24
					FG	87,500	21.0	111,500	32
				Cr					
48-051	1.17	13.0	0.49	1.99		71,750	8	127,500	50
46-532	0.95	13.3	0.60	2.04		71,250	15	113,525	44
				Ni					
45-262	1.17	12.7	0.53	3.56		..	..	114,000	48
46-483	0.91	13.3	0.53	3.38		73,750	24	103,500	44
						88,750	28	124,900	57
XG137	0.86	15.2	1.02	4.04		72,000	27	100,500	47
						98,000	35	119,250	62
45-495	0.84	12.5	0.48	3.46	CG	..	..	106,250	50
					FG	..	..	121,750	60
46-572	0.80	13.5	0.53	3.61		77,500	26	94,625	42
						95,250	33	121,250	61
50-313	0.79	13.4	0.72	3.57		91,000	35	122,750	56
				Mo					
50-553	0.89	14.1	0.54	0.99		100,000	30	136,750	67
XG135	0.81	15.0	0.98	1.08		87/117,500	30/38.5	122/136,500	57/65
50-546	0.75	13.9	0.58	0.94		107,500	40	131,500	59
50-557	0.93	14.2	0.61	1.95		105,750	29	132,000	54
50-521	0.77	14.0	0.54	2.00		112,500	35	128,750	51
50-019	0.69	14.2	1.19	2.42		89/109,500	26/30	116,980	31

CG = coarse grained. FG = fine grained.

and finally the unfused base itself will be affected. The lower carbon and alloy grades in Table 3 will indicate the pattern of weld deposit response. The base metal is usually the higher carbon standard manganese steel, which is more likely to lose toughness during welding than is the deposited metal.

The heat-affected zone close to the weld junction will be heated briefly into the austenitizing range. The heating will cause expansion that, if hindered, is converted to plastic flow which will have the effect of shortening the original length of the welded face. This leads to warping as the shortened face contracts during cooling. The usual effect is to make the weld deposit area concave; it can be minimized by good technique or heavy restraint. With restraint severe tensile stresses build up in the welded zone during cooling. They readily cause cracks, especially in the underbead area where slow cooling from white heat permits carbides to precipitate in grain boundaries (Fig. 1).

The most dangerous range is near 1800° F where manganese steel is both weak and brittle, and very likely to crack in tension. If the deposit is pounded at this point, the peening will create compressive stresses that not only reduce the tendency to crack but oppose the warping tendency. This is the reason for the standard advice to peen manganese steel welds promptly after welding. The peening is most effective when the weld is nearly white hot, but this is generally impractical.

The effects of alloying elements on bead cracking will be discussed under the section on Weld Defects.

ELEVATED TEMPERATURE PROPERTIES

Austenitic manganese steel is not rec-

Table 4—Tensile Properties of Several Austenitic Manganese Steels at 1800° F

Heat No.	C, %	Mn, %	Si, %	Ni, %	Mo, %	P, %	S, %	Ultimate tensile strength, psi	Elongation, % in 2 in.	Reduction of area, %
49-388	1.13	11.9	0.49	..	..	0.04	....	7,250	1.5	1.5
49-418	0.67	12.8	0.72	3.5	..	0.05	....	7,500	1.0	3.9
50-553	0.89	14.1	0.54	..	0.99	0.05	0.016	7,300	10.8	10.8
50-546	0.75	13.9	0.58	..	0.94	0.04	0.015	8,000	25.7	32.8
50-557	0.93	14.2	0.61	..	1.95	0.05	0.009	10,000	30.9	38.8
50-521	0.77	14.0	0.54	..	2.00	0.05	0.011	8,900	33.3	40.4
50-520	0.90	13.9	0.55	..	2.00	0.09	0.010	6,750	2.0	3.1
50-545	0.90	13.9	0.58	..	1.79	0.07	0.021	8,100	4.4	3.1
50-558	0.79	14.4	0.61	..	2.00	0.08	0.018	9,000	12.4	14.8
50-552	0.74	13.9	0.51	..	1.92	0.08	0.014	8,700	12.9	15.6

Room temperature tensile properties for comparison

Heat No.	C, %	P, %	Mo, %	As-cast: Yield strength, psi	As-cast: Ultimate tensile strength, psi	As-cast: Elongation, %	As-cast: Reduction of area, %	Water quenched from above 1800° F: Yield strength, psi	Water quenched from above 1800° F: Ultimate tensile strength, psi	Water quenched from above 1800° F: Elongation, %	Water quenched from above 1800° F: Reduction of area, %	Hardness, Bhn
50-553	0.89	0.05	0.99	52,000	100,000	29.5	22	54,000	136,750	67.0	38	207
50-546	0.75	0.04	0.94	48,750	107,500	39.5	30	52,500	131,500	59.0	38	207
50-557	0.93	0.05	1.95	56,750	105,750	28.5	21	58,000	132,000	53.5	34	207
50-521	0.77	0.05	2.00	55,000	112,500	35.0	26	54,250	128,750	50.5	33	183
50-520	0.90	0.09	2.00	55,750	95,000	24.5	21	57,750	120,000	43.0	29	188
50-545	0.90	0.07	1.79	57,000	102,250	26.0	20	58,750	128,000	48.5	32	196
50-558	0.79	0.08	2.00	54,500	101,000	29.0	22	57,000	127,500	51.0	33	207
50-552	0.74	0.08	1.92	54,000	111,000	36.0	25	57,000	125,740	51.0	33	202

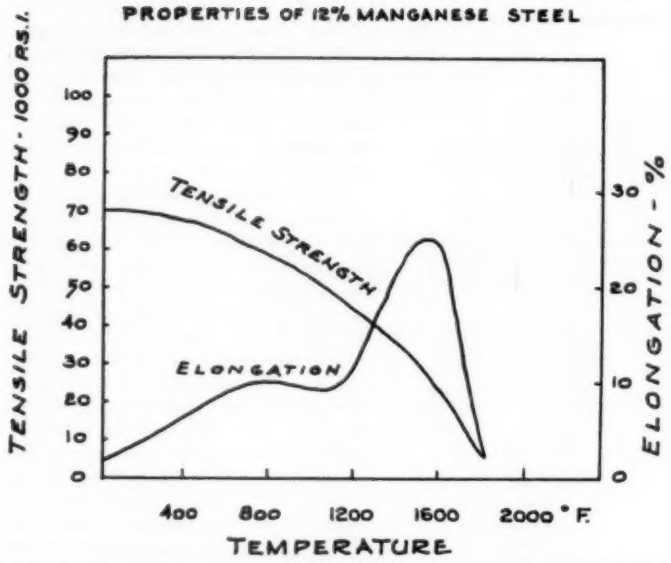

Fig. 6 Tensile properties of Hadfield's manganese steel up to 1830° F after F., Roll[5]

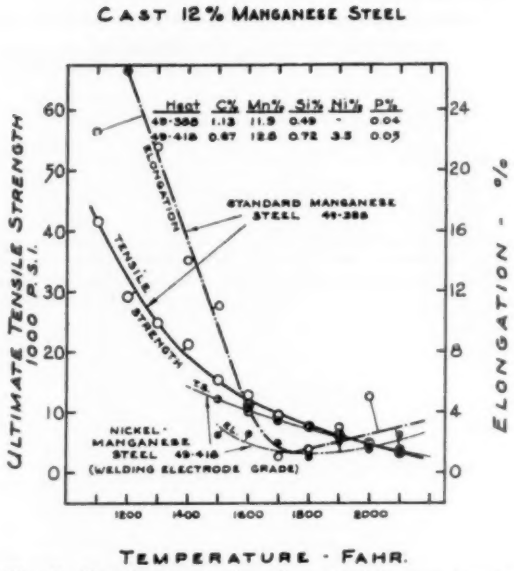

Fig. 7 Tensile properties of austenitic manganese steel of the standard and Ni-Mn welding electrode grades from 1100 to 2100° F

ommended for service above 500° F, so there are little data available relating to high temperature behavior. However, weld deposits must cool through the red heat range and adverse effects thus become pertinent. It is not generally known that manganese steel is quite hot short in the vicinity of 1800° F. Figure 6 from a German article[5] and Fig. 7 from the files of the American Brake Shoe Co. Metallurgical Laboratory confirm that both the standard grade and the common nickel-manganese welding electrode type can be seriously hot short at 1800° F or above. This undesirable tendency may appear in weld deposits as visible cracks or unnoticed microscopic fissures. Were it not for the toughness of manganese, steel which confers a remarkable resistance to crack propagation, this hot shortness would have received more general recognition as an adverse property.

Phosphorus is known to have an adverse effect on hot ductility, and it was logical to look for a similar effect on bead cracking. When working with commercial electrodes, which usually contain about 0.045% phosphorus, no clear relation was detected. The evidence was probably too much obscured by variables to yield to anything but extensive statistical analysis.

Some influence of both carbon and phosphorus can be shown on ductility at 1800° F (Table 4). The comparative room temperature properties show the effect of both carbon and molybdenum on yield strength but the phosphorus variation seems insignificant. Crack count tests on this same group of alloys showed some variation, but it was not statistically significant (Table 16 in the section on Weld Defects).

WORK HARDENING

The remarkable capacity of 13% manganese steel to work harden is well known. Most metals will harden somewhat when cold worked, but few approach this alloy in such hardening, which is attributed to carbide precipitation. The manganese-rich austenite dissolves over 1% carbon readily at high temperature, but after cooling to ordinary temperatures there is a tendency to come out of this solid solution. High manganese makes this tendency so sluggish that it operates only with the aid of slow cooling, reheating or deformation. The first two cause carbide rejection that is concentrated along relatively few crystallographic planes and grain boundaries, as in Figs. 1*B* and 1*F*, where it is responsible for brittleness. Deformation causes small amounts of precipitation in a multitude of well-dispersed points and planes. The visible effect is to form slip lines, as in Fig. 1*I*. The carbides are so fine and well scattered that their tendency to embrittle is slight while their power to increase hardness and yield strength is marked.

Manganese steel work hardening is sometimes attributed to martensite formation. There is little or no evidence that this is true for the grades featured in this paper. Their martensite point is below room temperature. There is evidence of martensite in steels unbalanced by low manganese or other composition variables. Some types with lower alloy content will work harden very rapidly and might appear attractive for wear resistance. However, experimental data show that they are lower in both abrasion resistance and toughness than the standard grades.

Toughened manganese steel has a hardness below 200 Brinell. Work hardening, which is in proportion to local deformation, can raise this up to around 550 Bhn, though the top values usually seen after service are in the 400–500 Bhn range. It is common to credit wear resistance to this hardening. This may not be correct.

Where an abrasive is harder than the soft austenite, but softer than the work-hardened alloy, it is reasonable to expect abrasion resistance to improve. This should be the case with such rocks as dolomite or limestone. The pattern has not been proved for harder minerals such as feldspar and quartz. With reliable laboratory tests for both high stress grinding abrasion and low stress scratching abrasion, it could not be established that drastic work hardening would decrease wear against quartz sand outside of the moderate experimental error range of the tests.

The case for metal to metal wear is different. Even carefully finished surfaces are microscopically rough. If one of them is hard, the roughness can have a filelike action on the mating face unless separated from it by a competent film of lubricant. If the faces are soft, the minute projections can be mashed down under stress. Intimate contact of the increased areas that result from this deformation can lead to seizure, galling and related manifestations of welding wear. Hardness seems to minimize such welding wear, as will films that prevent intimate metallic contact.

Manganese steel is not likely to have the filelike action because its original rough spots are soft. Deformation easily occurs to provide a smooth surface, but simultaneously this surface is so hardened that it resists welding wear. This case harden-

ing, together with smooth conformation of mating faces, provides a unique combination for metal to metal wear. Some bearings are designed on this basis with only water as a lubricant (or coolant).

RESISTANCE TO ABRASION

The ability of austenitic manganese steel to withstand scratching or low stress abrasion is not marked. Against quartz sand it wears about half as much as soft steel. Manganese steel castings are important parts of many dredge and materials handling pumps, where they are subject to erosion by sand laden water. They may also have to withstand the impact of large stones. Cast pipe sections, elbows and the like made of this alloy have a similar status.

This type of wear has been best reproduced in the laboratory by means of a rubber wheel test using sand as a loose abrasive. It may be termed erosion for simplicity and because velocity is an important variable, but in the laboratory test it is measured as low stress scratching abrasion. For manganese steel the wear factors* from this device ranged from 0.41 to 0.74 vs. 1.00 for mild steel and averaged 0.545 for 12 specimens. These included nickel-manganese and 2% chromium modifications as well as the standard grade. Several of the specimens were work hardened up to Rockwell C45 by hammering without significantly changing the erosion resistance. Arc weld deposits from a coated electrode depositing manganese steel containing about 0.80% molybdenum gave an average dry sand erosion factor of 0.62. Comparisons with other materials and a schematic diagram of the test may be found in previous publications.[6,7]

The abrasion factor against wet quartz sand in a grinding or high stress abrasion test[8] that correlates with ball mill service is 0.80 ± 0.05 and, as in the low stress test above, work hardening does not significantly change it when quartz sand is the abrasive. Arc weld deposits of the various manganese steel types fall within the same range.

Many alloys have better abrasion resistance than manganese steel, but under heavy impact conditions they are prone to failure by cracking and fracture. Manganese steel because of its toughness has a distinct advantage here and is generally accepted as the best material to withstand combinations of abrasion and heavy impact. Dynamite has been known to explode inside the casing of a pump like those mentioned above, without cracking the manganese steel shell.

Occasionally it will be claimed that a certain hard-facing overlay will protect manganese steel from abrasion long enough for it to work harden and develop its own wear resistance. Such statements reflect a lack of knowledge of manganese steel and its abrasion resistance, which under severe conditions changes little if any with work hardening. Also an overlay that protects from wear will also tend to prevent work hardening.

Practically, it is desirable to protect manganese steel from surface abrasion, since its salient advantage is toughness as a body of metal. Such protection will be most effective when well engineered, as described in "Hard Facing for Impact."[9] One of the best overlays for manganese steel is a carefully formulated martensitic iron, selected on the basis of abrasion tests and careful wear analysis in the field.

Brittle overlays like composite tungsten carbide weld deposits,[10] high chromium irons,[6] and martensitic irons[9] are not recommended for thicknesses over 3/8 in. If previous wear requires more weld metal to restore dimensions, the extra thickness should be built up with an austenitic steel electrode.

FLOW RESISTANCE

Plastic flow under impact is the first and chief evidence of wear in many applications of manganese steel. Railway trackwork is the outstanding example. Welding is done here primarily to restore the original dimensions after batter has become excessive.

The resistance to flow of manganese steels is affected by composition. Carbon silicon, molybdenum, chromium and other elements can decrease flow, though usually at the expense of ductility. Chromium is probably the element most frequently employed for this purpose. Practically, the merit of chromium-manganese steel, as evidenced by field tests, is obscure. Favorable reports can be matched by those where it has provided no advantage over the standard grade. Some evidence indicates that the lowering of ductility by chromium is undesirable.

When carbon is lowered in the electrode compositions, the result is decreased flow resistance (and yield strength, which is related). Nickel has practically no effect on yield strength and consequently the nickel-manganese weld deposits are lower in these properties than standard manganese steel.

In contrast, molybdenum can raise yield strength and thus compensate for lower carbon. This appears on several tables herein, and the associated advantage in flow resistance under repeated impact has been shown graphically in a previous publication,[9] where a 2% molybdenum-manganese steel has about 20% less flow than a 3.5% nickel-manganese of practically the same carbon content, after the same total impact.

This modest superiority of the molybdenum-manganese grade is the primary advantage in comparison with the earlier nickel-manganese type. It is important because the properties involved are closely related to many service requirements.

Resistance to flow is rapidly improved by the work hardening of manganese steel under impact. The flow curves just referred to[9] show this nicely. Of interest to welders is the fact that the likelihood of embrittlement (from reheating) increases at the same time. Thus work-hardened metal is more likely to be damaged by welding heat. It is advisable to remove the work-hardened zone by grinding before welding.

HEAT TREATMENT

Standard manganese steel, containing about 1.15% carbon, is relatively brittle after casting or forging because of slow cooling. The brittleness is due to carbide precipitation, austenite transformation or both (as shown in Fig. 1). Before use it is toughened in the manufacturing plant by water quenching from above 1800° F. During this treatment oxidation by furnace gases usually causes loss of some carbon and manganese in a thin surface layer. This is evidence by a magnetic and somewhat hardened skin after quenching.

Except in a properly equipped plant it is inconvenient to heat treat weldments to obtain maximum toughness. Moreover, if the weld deposits contain defects such as cracks, oxidation of these can lead to more structural damage than would result from the combination of defects and welding heat. Weld metal composition and welding techniques should minimize any need for reheat treatment.

In the unusual case where manganese steel is gas welded, it is unlikely that reasonable toughness will be obtained without a subsequent heat treatment. If the weld is sound, it may be practical to heat the whole weldment to yellow heat and then to quench it in water, thereby restoring toughness. This procedure is not recommended for arc welds because they are more likely to contain defects susceptible to penetrating oxidation.

SLOW COOLING

If postweld heat treatment is to be avoided the weld deposit should develop acceptable properties after relatively slow cooling. The contribution of low carbon and of some alloying elements to properties after slowly cooling cast test specimens in the mold is shown in Table 3.

It will be noted that ductility, as expressed by tensile elongation, rises as carbon decreases for the slowly cooled specimens, whereas it shows a reverse trend below 0.90% carbon for the toughened condition. At the same time the slowly cooled specimens show only a frac-

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* The wear factors reported here are ratios of weight loss against annealed SAE1020 Steel.

$$\text{Abrasion factor} = \frac{\text{wt. loss of specimen}}{\text{wt. loss of SAE1020}}$$

The two materials are run in the grinding abrasion machine at the same time.

tion of the ultimate strength of the toughened conditions.

Either nickel or molybdenum will increase the elongation and ultimate strength of the lower carbon ranges. This occurs for both rapid and slow cooling. Thus these elements contribute to inherent toughness. Quenching seems necessary to obtain it in full measure, but the 0.75–0.90% carbon range of both alloy grades develop good properties even when slowly cooled. The strength conferred by molybdenum appears higher than that from nickel and this indicates a modest superiority in toughness. At the same carbon level molybdenum carries the asset of a higher yield strength also.

Manganese steel tensile properties are sensitive to the grain size of individual crystals, which in turn is controlled by cooling rates during solidification. Toughness is higher with small crystals from fast cooling. Where pairs of values appear in Tables 3 and 5 they reflect the difference when both coarse- and fine-grained specimens are produced from the same melt.

Comparison of the cooling rates of weld deposits with those represented in Table 3 is pertinent. The mold-cooled specimens require about 75 min to go from 2100 to 600° F and have a rate of about 45° F per minute at 1600° F, and 12° F per minute at 1000° F. Only a few weld deposits will cool as slowly as this. The inherent properties of welds would, therefore, be expected to lie between those of the slowly and rapidly cooled specimens in Table 3. The cooling rates of arc welds under many conditions, for comparison, may be found in a Welding Research Council report.[11] The actual composition of the deposited metal, as contrasted with that of the electrode, must be taken into account when estimating properties from these data. Probable alloy losses are discussed later under Composition Changes. Properly formulated electrodes take into account alloy losses and composition changes with normal welding procedure, but since both electrode and deposit analyses may be quoted in descriptive literature, it is important to note which is given.

Table 5—The Embrittlement of Cast Austenitic Manganese Steels by 48 Hr at 800° F

	Chemical composition, %					*Toughened by water quenching from 2000° F* —*As-toughened*—		*Reheated 48 hr at 800° F*	
Heat No.	*C*	*Mn*	*Si*			*Ultimate tensile strength, psi*	*Elongation, % in 2 in*	*Ultimate tensile strength, psi*	*Elongation, % in 2 in*
44-038	1.31	12.9	0.33		CG	111,250	35.5	86,250	1.0
					FG	134,500	42.5	96,250	1.0
45-235	1.20	13.0	0.50		CG	110,000	49.0	98,750	3.5
					FG	129,400	48.0	99,500	2.0
45-347	1.03	12.9	0.52		CG	108,750	39.5	93,750	11.0
					FG	131,750	46.0	109,500	12.5
				Ni					
45-262	1.17	12.7	0.53	3.56	CG	112,500	48.0	88,000	3.5
					FG	115,250	48.0	90,000	4.5
45-495	0.84	12.5	0.48	3.46	CG	106,250	49.5	105,250	47.5
					FG	121,750	60.0	127,000	65.0
				Mo					
44-100	1.15	12.8	0.51	0.98	CG	130,000	48.5	107,750	19.0
					FG	138,000	49.0	108,250	23.0
51-362	0.80	13.8	1.08	1.07	MG	124,500	49.0	127,000	56.0

CG = coarse grained. MG = medium grained. FG = fine grained.

REHEATING EMBRITTLEMENT

In manganese steel weldments there are three zones influenced by temperature: the bead, the underbead area and the base metal near the weld. The fusion zone and the bead itself cool from the molten state, and their properties (if they are free from defects) depend on the factors just discussed. Immediately under the bead the base is heated well into the austenitizing temperature range and its subsequent properties also depend upon these factors.

In the heat-affected zone further from the weld the base will be reheated to below the austenitizing range. (This status also applies to the earlier beads of a multipass or multilayer weld.) This creates a tendency to embrittlement by causing carbides to precipitate on crystal planes as well as grain boundaries (Fig. 1*H*). This embrittlement is speeded up by temperature; it may require 33 days at 600° F, 2½ hr at 800° F and only 5 min at 1100° F. It is accelerated if the manganese steel has been previously work hardened. In the extreme case a few minutes of careless welding can change the ductility of this heat-affected zone from perhaps the 40% elongation level down to less than one percent, exchanging the outstanding toughness of manganese steel for the brittleness and moderate strength of a good grade of gray cast iron. Of course, such embrittlement is not always followed by failure; cast-iron properties are good enough for many engineering uses. However, if the toughness of manganese steel is really needed, it is important not to destroy it by improper welding. Present good practice involves the use of temperature indicating crayons to warn against overheating. The zone ¼ in. from the weld should not exceed 500° F. Even with this control the base metal between this point and the bead will suffer temperatures between 500 and 2450° F for various times. Multipass welds will repeat the exposure to heat. The time factor is very important.

As an example, a V butt weld between 1-in. thick bars 5 in. wide requires about 8 to 10 passes to fill a 110° groove.* Each pass used three 9-in. electrodes and the three consecutive beads required a total of about 3 to 3½ min for deposition (140–150 amp, bare 3/16-in. electrodes). Without peening, which was not used for this group, the entire weld required about 30 min of actual welding time and near 2 hr total, as each bead was fan cooled to below 400° F. After deposition each bead is visibly red hot for about one-half to two-thirds of its length. Thus some base metal is obviously heated up to about 1200° F for a maximum of perhaps 15 to 20 min. This is likely to cause considerable embrittlement unless the manganese steel base is formulated for special resistance to reheating.

Neither interpass cooling nor peening is popular with welders. The time they consume adds considerably to the cost of welding. There is a strong temptation to weld continuously, allowing the whole weldment to get red hot. This practice has not been recommended by manufacturers, probably because it can lead to serious embrittlement, but there is a critical set of conditions where it may be satisfactory. With preheat (which is not usually advisable because of reheating embrittlement) combined with hot welding, the thermal gradients that cause bead cracks are reduced and sounder welds may result. If the entire weldment is hot enough when welding is complete (above 1800° F is desirable), and if it is very quickly cooled by water quenching, there is a good chance of re-establishing the original tough austenitic structure. If the final temperature is too low, or if cooling is too slow, relatively brittle structures will result. At what point the reheated structures of the recommended practice are superior in toughness has not been established clearly. It should be recognized that this is primarily a base metal problem, though the early beads of a large weld are also involved.

The lower carbon alloy modifications of manganese steel as used for electrodes are somewhat less affected by reheating than the standard grade (Table 5). For this reason they make a better, less sensitive base for welding. In a few cases the nickel-manganese type has been specified for trackwork and other castings. How-

* This procedure was used to obtain all-weld-metal tensile specimens.

Table 6—Composition Changes of Austenitic Manganese Steels During Arc Welding

	Carbon, %			Manganese, %			Silicon, %		
	Rod	*Deposit*	*Loss*	*Rod*	*Deposit*	*Loss*	*Rod*	*Deposit*	*Loss*
				Bare electrodes					
Range	0.62–1.04	0.51–0.86	...	12.6–15.0	10.0–14.1	...	0.60–1.56	0.35–1.11	...
Avg.		(n = 10)	0.10		(n = 10)	2.1		(n = 10)	0.29
Est. std. dev.			0.096			1.25			0.096
				Coated electrodes					
Range	0.72–0.87	0.41–0.71	...	12.8–14.3	9.2–13.8	...	0.63–1.09	0.34–0.95	...
Avg.		(n = 8)	0.23		(n = 8)	2.3		(n = 8)	0.35
Est. std. dev.			0.06			0.88			0.20

ever, the modifications have not been popular, probably because they cost more.

COMPOSITION CHANGES DURING WELDING

A comparison of analyses of electrode core wire vs. the matching deposited metal appears in Table 6. In round numbers it is indicated that about 0.20% carbon, 2% manganese and 0.3% silicon are lost during welding with bare electrodes, while the loss from coated electrodes is somewhat more. The standard deviations indicate the variability of these losses, which of course are controlled considerably by welding conditions.

Nickel is not much affected by transfer across the arc; practically 100% recovery can be assumed. Molybdenum is also recovered efficiently under favorable conditions, losses ordinarily being below 5% of the original amount.

The role of nitrogen is not clear. It may range from 0.02 to 0.06% in the electrodes, and several checks indicate that it about doubles in the weld deposit. Some early tests of manganese steel melts to which nitrogen was intentionally added gave no conclusive effects on tensile properties. Possibly it adds to the carbon effects, as might be expected.

These data on changes are useful for the interpretation of the tabular data relating properties to composition in this paper.

WELD DEFECTS

Wherever toughness is at a premium the presence of weld defects such as slag inclusions, incomplete fusion, gas holes, bead cracks, fissures or brittle structures can be a serious liability. Slag inclusions and incomplete fusion are in the hands of the welder and are not confined to manganese steel welds. Brittle martensitic structures may result from poor judgment in selecting materials and have been discussed already (Fig. 1*G*). Gas and cracking can be related to both materials and welding techniques that merit further discussion.

Some years ago an investigation began because field complaints of cracking could not be correlated with chemical analyses, plant welding tests or other quality control procedures. Several factors related to cracking were tentatively identified only to have further work fail to substantiate their importance.

As a laboratory stage of this investigation a variety of currently available electrodes were used. These included all of the important commercially available types and served to demonstrate that the cracking and weld defect problem was common to the different manufacturers, though it may not have been clearly recognized.

To standardize procedure a 3 to 3$^1/_2$-in.-long bead was laid on a $^7/_8$-in. thick x 2 x 4-in. block of cast manganese steel. The deposits were cut longitudinally and etched to reveal bead cracks. It will be recognized that a simple bead on a small block is not a particularly severe test of cracking tendencies.

Using this procedure the crack counts of 154 welds, representing 33 different electrodes, were examined. The average number of cracks per electrode lot ranged from 0 to 34, with individual counts as high as 56. The range from repeated testing of a single electrode type was as high as 56 cracks, though usually it was not above the thirties. This group included a variety of bare and coated commercial electrodes, experimental compositions and coatings, and several inert-gas-shielded welds. The scatter bands shown by the range of crack counts for replicate specimens were so great that valid conclusions could not be reached by comparing averages. It became obvious at this point that statistical procedures would be required. Such techniques re-

Table 7—Cracks Observed in Austenitic Manganese Steel Weld Deposits

Direct current manual arc welding; duplicates randomized in welding order separately; stringer bead deposits on $^7/_8$- x 2- x 4-in. blocks of Mn steel

Rod Lot	*Diam., in.*	*Chemical composition, %*						*Type*	*Amp*	*Observed bead cracks*			
		C	*Mn*	*Si*	*Ni*	*Cr*	*Mo*			*Visible on etched section*	*Visible uncut*	*Gas holes*	*Under-bead cracks*
A	$^3/_{16}$	0.39	12.9	0.55	3.3	4.6	0.49	Cast	160	5	0	21	9
									160	1	0	33	6
B	$^3/_{16}$	0.62	12.6	0.84	3.1	..	..	Drawn	160	4	1	4	10
									160	5	3	15	8
C	$^3/_{16}$	0.69	14.2	1.03	..	0.2	2.42	Cast	160	7	0	6	19
									160	12	4	19	14
D	$^3/_{16}$	0.72	13.0	0.63	3.7	0.2	..	Cast	160	3	0	21	15
									160	3	1	29	27
E	$^3/_{16}$	0.72	13.2	1.05	3.7	..	..	Drawn	160	9	2	15	13
									160	8	2	13	13
F	$^3/_{16}$	0.75	13.1	0.54	3.6	0.1	..	Cast	160	2	1	7	11
									160	10	3	18	12
G	$^3/_{16}$	0.76	14.4	1.14	3.9	..	..	Drawn	160	9	2	12	10
									160	9	2	7	12
H	$^{17}/_{64}$	0.78	14.1	1.29	4.1	0.2	..	Drawn	180	7	4	6	14
									180	8	1	7	9
I	$^3/_{16}$	0.81	13.0	0.93	3.6	..	..	Cast	160	7	1	14	15
									160	8	0	17	8
J	$^1/_{22}$	0.96	13.7	0.97	4.1	..	..	Cast	140	4	1	22	9
									160	14	4	56	9

Beads were approximately $^1/_4$ in. thick and $^3/_4$ in. wide, 0.20 ± 0.03 in. above block. Crater areas not included in crack count.

quire randomization of specimen preparation and testing to prevent the unsuspected operation of systematic but unrecognized variables.

A subsequent group of lot comparisons was made in duplicate, each replicate set being randomized separately. The crack counts were classified in more detail and gas porosity was also recorded. Part of this group is reported in Table 7 to show that many of the cracks would not be visible on the surface, to indicate the prevalence of underbead cracks and to show the presence of gas porosity.

There will be a temptation to draw conclusions about relative merits of the alloys in Table 7. This should be avoided. Without involving the detailed mathematics of statistical comparisons, note that the average range (spread between the duplicate values) in Table 7 is 3.1 cracks. For pairs the estimated standard deviation is $3.1 \times 0.89 = 2.75$ cracks. The 95% probable range about the average value is ± 2 std. deviations or 5.5 cracks. The overlap between the lowest average (3 cracks) plus 5.5 and the highest ($9^1/_2$ cracks) minus 5.5, demonstrates that we are not justified in concluding that the observed differences are greater than could occur by chance. The data may provide hints, but not proof.

One hint involves the difference in crack count for the two welding current levels used for lot J. Another stems from differences in bead size from some of these comparisons. It also seemed advisable to employ more replicates, and, since manual welding is a potential source of variation, to substitute automatic arc length and traversing control. It was finally decided that automatic welding and instrumentation together with statistical planning and evaluation of experiments would be required to produce results that could be used with confidence.

AUTOMATIC WELDING PROCEDURE FOR CRACKING STUDIES

An Auto-Arc welding head and controls were mounted on a rigid column where they could be positioned over a traversing table. The traversing mechanism was designed to provide stepless speed control that could be varied over a wide range or locked for constant speed. For the standardized crack count tests it was used to apply a simple stringer bead at a constant rate with good control of bead geometry.

The welding head was fitted with an improvised stick feeder attachment that permitted the use of ordinary bare and coated electrodes instead of coiled wire. The welding area was provided with guards against arc radiation, one of which was fitted with a port and a projection lens. The lens projected the image of the arc, considerably magnified, on a white screen where arc length could be measured and metal transfer conveniently observed.

A recording voltmeter, a recording wattmeter, an arc short-circuit counter and an integrating meter for showing the average percentage of time that the arc was extinguished were provided in addition to the indicating voltmeter and ammeter on the Auto-Arc control head. Provisions were made for checking the meters against laboratory standard cells by means of a precise voltage divider and a precision potentiometer.

The Auto-Arc head includes an a-c motor and a d-c motor. They are connected to feed rolls in such a way that they either work together or oppose each other, depending on the voltage fed to the field of the d-c motor from the arc. If arc voltage (and therefore arc length) rises above the control setting, the feed rolls will speed up and shorten the arc. If the arc voltage falls low the rolls will slow down or even reverse until the correct voltage is re-established. This operation tends to maintain approximately constant arc length and voltage. Arc current, which tends to remain approximately constant for a given generator setting, was regulated by the adjustment of the 300-amp motor generator.

One feed roll was provided with a contact arm and a microswitch that energized a marking pen in the recording meters. Each feed roll revolution made a reference mark on the time-voltage chart, from which it was easy to calculate burn-off rates.

Blocks of cast and heat-treated manganese steel, $^7/_8$ x 2 x $3^3/_4$ in., were selected for crack counts. A stringer bead was started on a separate lead block, continued down the center of the 4-in. length and off the end on to a run-off block. This located the crater at the end of the bead on the extra block where it could not introduce a variable into the test area.

After breaking off the lead and run-off blocks the 2-in. center part of the test block and bead were cut longitudinally through the center of the bead by an abrasive cutoff wheel. Cross sections adjacent to the 2-in. center provided bead contour measurements.

The cut faces were etched in 1:1 HCl at about 160° F, washed, dried and projected at 2 × magnification on to a tracing paper screen. The outline and the visible cracks were traced in ink. (This procedure was more satisfactory than photography.) The accuracy of the tracing was then checked by direct observation of the etched face at higher magnification through a binocular microscope. Evidence of transverse bead cracks and gas porosity is provided by this method, but it is not a searching procedure for the latter.

STATISTICAL METHODS

The experiments were planned and executed in groups to permit valid calculations of the analysis of variance and the significance of the difference between means.[12] This involved replication of each series two or three times and randomization of the order of welding and testing for each series.[13] The arc power plus bead size study and the rod vs. base block comparison were set up as factorial experiments.[14] The other studies were usually lot comparisons. The statistical approach is invaluable because it provides estimates of experimental error that indicate clearly how much confidence in the results is justified.

The first experiment with full instrumentation involved examination of arc power and bead size effects with bare electrodes. Preliminary observation of metal transfer and arc action, which was facilitated by projecting the image of the arc, revealed a changing pattern related to arc length, as sketched in Fig. 8. The relative smoothness of the mid-range action suggested that it might be optimum for welding so the experiment was planned to bracket this condition. The data are summarized in Table 8 and significant trends appear in Fig. 9. The evidence contrasts sharply with the vague indications of the preceding comparisons of electrodes, since the probability (column P) that the observed variations attributed to power and bead size are due to chance is less than one in twenty. An unexpected development is the association of the short, frequently short-circuited arc with low crack counts.

The instrumentation and nominal machine settings* of welding generators are frequently unreliable. This makes the burn-off rate data especially valuable since they can be used as a good index of arc power. The evidence suggests strongly that burn-off rates below 12 in. per minute be employed to avoid bead cracking. Such rates are easily measured and when used with a short arc and large beads promise considerable freedom from weld bead cracks.

A similar experiment with coated electrodes was made later, as detailed in Table 9. However, bead volume was not included as a variable. Traverse speed for each power level was adjusted to provide an approximately constant bead size. Obviously arc power is a potent variable that affects the cracking tendency of coated electrode deposits as it does those from bare rods. Also, the coating in this case has not minimized cracking (compare the medium power data with the low power, small bead group in Table 8). It has operated to make low power welding easier and it lowers arc voltage for a given generator setting. For comparable burn-off rates this particular coating offers no obvious advantage. This seems to justify the usual preference in the field for bare

* The generator settings in the following tables (e.g., N195R) refer to the normal voltage-current adjustment (N), the nominal current setting, in amperes and the polarity (R = reversed, with electrode positive).

Erratic arc action: **Top of advancing bead is above molten globule, permitting bead turbulence to cause frequent short circuits and high percentage of arc outage.**

Smooth arc action: **Bead-globule relations are such that transfer is prompt when globule enlarges, but not premature. Low arc outage.**

Long globule action: **Transfer is delayed too long, leaving solid electrode too far from deposit. Arc may extinguish after globule transfers, but otherwise is stable.**

Fig. 8 Manganese steel arc weld metal transfer as observed with $^3/_{16}$-in. diam bare electrodes and d-c motor generator (reversed polarity)

Table 8—Arc Power and Bead Size vs. Bead Cracking

A 3 x 3 x 2 factorial experiment, 18 welds; welding order of each replicate randomized

Electrode: Bare $^3/_{16}$-in. diam. drawn Ni-Mn wire.
Base: Cast, water-quenched blocks, $^7/_8$ x 2 x 3$^1/_2$ in.

	Chemical composition, %						
	C	*Mn*	*Si*	*Ni*	*Cr*	*P*	*S*
Electrode	0.79	13.04	1.00	4.03	0.22	0.041	0.010
Base	1.13	12.76	0.57	..	..	0.038	0.024

	Low power	*Medium power*	*High power*
Generator setting	N165R	N195R	N240R
Average voltage, v	29.4	31.0	32.2
Average current, amp	130	167	219
Average power, kw	3.81	5.15	6.47
Burn-off rate, in. per min.	12.76	16.23	20.75

	Bead cracks in etched 2-in. longitudinal section (and arc short circuits per 2 in. of bead)			*Bead size avgs*
	Low power	*Medium power*	*High power*	
Small beads				
(x-sect. area 0.123 cu in.) cracks	0 and 1	4 and 3	13 and 8	4.83
(x-sect. area 0.123 cu in.) avg	0.5	3.5	10.5	
Short circuits	(144 & 143)	(141 & 143)	(107 & 73)	
Medium beads				
(x-sect. area 0.155 cu in.) cracks	0 and 0	0 and 0	2 and 3	0.833
(x-sect. area 0.155 cu in.) avg	0	0	2.5	
Short circuits	(220 & 230)	(142 & 155)	(120 & 94)	
Large beads				
(x-sect. area 0.20 cu in.) cracks	0 and 0	0 and 0	0 and 7	1.167
(x-sect. area 0.20 cu in.) avg	0	0	3.5	
Short circuits	(367 & 286)	(182 & 208)	(110 & 111)	
Power level Avgs	0.167 (235)	1.167 (162)	5.5 (103)	

Analysis of variance

Variation source	*Degrees of freedom*	*Sum of squares*	*Mean square*	*F ratio*	*P*
Arc power	2	96.44	48.22	11.27	0.01–0.001
Bead volume	2	59.08	29.54	6.90	0.05–0.01
Power volume interaction	4	33.58	8.40	1.96	0.2 –0.1
Experimental error	9	38.50	4.28		
Grand total	17	227.60			
Revision lumping $P \times V$ with experimental error					
Arc power	2	96.44	48.22	8.69	0.01–0.001
Bead volume	2	59.08	29.54	5.32	0.05–0.01
Experimental error	13	72.08	5.55		
Grand total	17	227.60	83.31		

rods where build-up applications are involved. Casual inspection would rate the welds in this experiment as of good quality since all but a few of the cracks counted were small and did not reach the surface. They are considered no more serious than the underbead cracks that occur with all of the welds so far reported.

These experimental results demonstrate clearly that arc power and bead size must be closely controlled in any valid comparisons of different electrodes. The next factorial test compared two lots of commercial bare electrodes on two base materials. One base group was from a single carefully identified heat while the other group was from a bin where heats were mixed without marking, as was the case with much of the earlier work. The test conditions were selected to be severe and to produce plenty of cracks. Each condition was replicated three times. The data appear in Table 10.

Whenever averages are compared there is the possibility that the observed difference is due to chance. For electrodes A and B there is more than a 20% probability that this is true. The hint that A is better requires further proof. Even the conclusion that most of the variance is due to the base blocks has only something more than a 90% probability of being true, and this margin is not generally accepted as good enough for engineering work. (The 95% probability, or P = 0.05, is the usual minimum criterion for valid engineering conclusions.) However, the apparent influence of the base metal was sufficient to make this an indicated variable in subsequent work.

Table 9—Arc Power vs. Bead Cracking for Coated Electrodes

A 3 x 3 factorial experiment 9 welds; welding order of each replicate randomized

Electrode: Coated $^3/_{16}$-in. diam. drawn Ni-Mn wire.
Base: Cast water-quenched blocks $^7/_8$ x 2 in. × 4 in.

	Chemical composition %						
	C	*Mn*	*Si*	*Ni*	*Cr*	*P*	*S*
Electrode	0.74	13.10	1.04	3.68	..	0.044	0.013
Base	1.15	12.55	0.85	..	0.49	0.059	0.016

	Low power	*Medium power*	*High power*
Generator setting	N125R	N160R	N195R
Average voltage v	23.7	24.4	25.0
Average current amp	103	143	190
Average Power kw	2.43	3.48	4.75
Average Burn-off rate in. per min.	7.68	10.21	12.70
Average Arc outage, %	5, 14, 16	9, 11, 10	7, 10, 11
Average Short circuits per 2 in.	247	183	141
Average traverse speed, in. per min.	2.45	3.88	6.13
Average Bead x-sec. area, sq. in.	0.113	0.104	0.100
	Crack counts in 2-in. etched long section		
Individual	0, 0, 2	35, 25, 29	40, 43, 47
Avg.	0.667	29.7	43.3
±1.0 std. dev.	±0.943	±4.11	±8.50

COMPARISON OF ELECTRODES

The comparison procedures just described were subsequently used to evaluate many electrodes, most of which were experimental. This program will not be reported in detail here since the present paper is intended to cover current commercially available electrodes. However certain features that are significant in relation to cracking control justify empha-

Table 10—A Comparison of Electrodes and Bases on Cracking Tendencies of Austenitic Nickel-Manganese Welds

A 2 x 2 x 3 factorial experiment, 12 welds; welding order of each replicate randomized

Electrode: Bare $^3/_{16}$-in. diam drawn Ni-Mn wire
Base: Cast, water-quenched blocks, $^7/_8$ x 2 x $3^3/_4$ in.

	Chemical composition, %						
	C	*Mn*	*Si*	*Ni*	*Cr*	*P*	*Identification*
Electrode A	0.79	13.04	1.00	4.03	0.22	0.041	S1445
Electrode B	0.76	14.36	1.14	3.90	..	0.048	R2569
Base group C	1.13	12.76	0.57	..	0.08	0.038	50-322
Base group D	miscellaneous, not identified						

Westinghouse Flexarc 300-amp unit set at normal—195 amp.
Reversed polarity. Control aim: 5000–5500 w arc power; 6.4 in. per min traverse speed.
Bead size: Avg—0.272 in. thick, 0.593 in. wide, 0.169 in. height above base. Range—0.26–0.28 in. thick, 0.56–0.63 in. wide, 0.16–0.18 in. high.
Calculated % base metal in bead: Range—43.2–51.8%; avg—47.6%.
Arc voltage—31 ± 1; current—168 ± 4 amp; power—5.1–5.3 kw, avg 5250 w.

	Bead cracks in etched 2-in. longitudinal section	
	Electrode A	*Electrode B*
Base group C	16, 5, 10	23, 15, 14
Base group D	16, 25, 20	28, 18, 15
Average base group C	10.3	17.3
Average base group D	20.3	20.3
Average of both	15.3	18.9

Analysis of variance

Preliminary assignable cause of variation	*Degrees of freedom*	*Sum of squares*	*Variance*	*Ratio to error*	*P*
Rods	1	36.76	36.76	1.21	Above 20%
Bases	1	126.76	126.76	4.18	Below 10%
Interaction	1	36.73	36.73	1.21	Above 20%
Error	8	242.67	30.33		
Total	11	442.92	230.58		
Revised					
Bases	1	126.76	126.76	4.01	(P = 4.96 for 5% and 3.28 for 10% levels of probability that the apparent variability due to bases is actually due to chance operation of experimental errors)
Error	10	316.16	31.62		
Total	11	442.92	158.38		

Table 11—Comparison of Various Coated Manganese Steel Electrodes

Welding order randomized separately for each replicate

Welding generator: Normal—160 amp—reversed polarity setting.
Conditions: 3.4 kw arc power, 3.88 in. per min traverse speed, about 24 v, 140 amp.

Electrode	Chemical composition, % C	Mn	Si	Ni	Cr	Mo	P	Bead cracks, avg
R824 (rod)	0.78	12.8	0.90	2.9	..	..	0.047	3.33
S3744 (deposit)	0.55	13.0	0.40	..	..	0.70	0.046	6.33
R2569 (rod)	0.76	14.4	1.10	3.9	0.26	..	0.048	13.00 (bare)
R3168 (deposit)	0.70	15.0	..	..	..	1.0	..	21.00
R2569 (rod)	0.76	14.4	1.10	3.9	0.26	..	0.048*	22.33 (coated)
S2006 (nominal)	..	11–14	..	3.5	..	..	..	22.33
R780 (rod)	0.86	13.9	1.0	..	..	..	0.093	28.00
R1202 (rod)	0.74	13.1	1.0	3.7	..	..	0.044	31.67
S4360 (nominal)	0.8	13	..	3.5	..	..	..	34.00

Electrode	Arc length, in.	Arc outage, %	Burn-off, in. per min	Gas holes, avg. in 2 in.	Underbead cracks	Bead cracks	Short circuits per 2 in., avg
R824	0.10	10	10.46	0.67	23, 9, 18	2, 0, 8	172
S3744	0.08	5	7.11	0.67	22, 23, 28	0, 0, 19	136
R2569	0.14	19	12.67	0.33	27, 39, 25	19, 12, 8	260
R3168	0.14	2	5.62	0.33	39, 26, 36	22, 13, 28	38
R2569	0.07	4	8.78	9.33	3, 27, 17	26, 16, 25	41
S2006	0.15	9	6.59	7.67	22, 16, 14	21, 12, 49	127
R780	0.12	12	10.01	1.67	4, 2, 19	43, 26, 15	191
R1202	0.08	13	10.16	2.33	9, 19, 13	24, 34, 37	208
S4360	0.08	4	8.84	7.33	4, 16, 5	49, 29, 24	60

* 0.043% P on a recheck.
Bead size range: Width 0.045 to 0.68; height 0.08 to 0.23; thickness 0.14 to 0.30; volume 0.122 to 0.246 cu in. (2 in. length).

sis, and part of the data to substantiate these conclusions will be included.

Most of these data were part of lot comparisons in which from one to two dozen electrodes were welded in random order, in triplicate, each replicate being randomized separately. The test conditions conformed to those in Table 10 for bare and Table 11 for coated electrodes. It should be recognized that they are severe and that less cracking ordinarily is encountered in commercial welding unless the beads are severely stressed by thermal contraction against effective restraint.

The coated electrodes in Table 11 came from five different sources. With two exceptions they do not seem to have minimized cracking. The two best, which are from two different manufacturers, are not significantly different (P = about 0.7), but there is a 90 to 95% probability that their apparent superiority over the others is not due to chance. There is a similar probability that the bare electrode is really better than the poorest coated lot, but any other comparisons within the poorer group are not statistically significant. The relation between arc short circuits and cracking shown in Table 8 does not appear to be confirmed in Table 11.

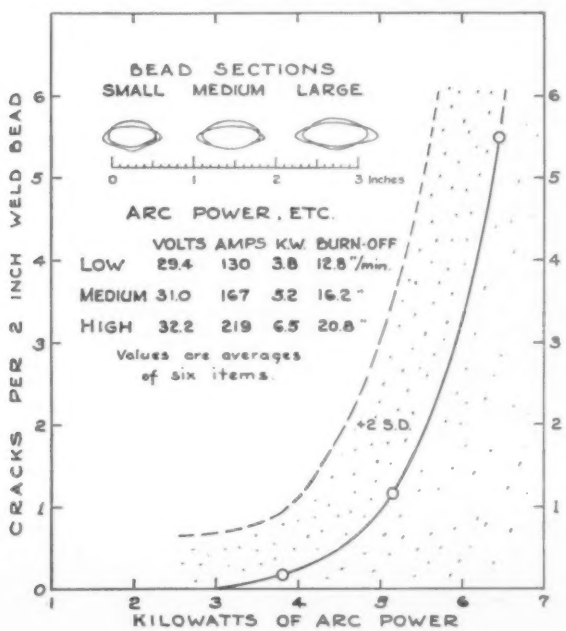

BEAD SECTIONS
SMALL MEDIUM LARGE
0 1 2 3 inches
ARC POWER, ETC.
VOLTS AMPS K.W. BURN-OFF
LOW 29.4 130 3.8 12.8"/min.
MEDIUM 31.0 167 5.2 16.2"
HIGH 32.2 219 6.5 20.8"
Values are averages of six items.
+2 S.D.
CRACKS PER 2 INCH WELD BEAD
KILOWATTS OF ARC POWER

Fig 9 Apparent dependence of weld bead cracking on arc power and bead size; stringer beads made with bare 3/16-in. diam austenitic nickel - manganese steel electrodes

Table 12 is a condensed summary of several lot comparisons of bare electrodes. One or both of the heats R2569 and S1445 were included as standards in each lot. By this extensive replication it was finally possible to prove statistically that S1445 is superior in crack resistance, even though casual comparisons on pairs of tests might show a reversed ranking. These were commercial nickel-manganese heats and provide a base line for judging other types.

Three molybdenum-manganese and one copper-molybdenum-manganese heat in Table 12 were significantly superior to the nickel-manganese heat R2569. This should not be attributed to the molybdenum as the molybdenum-manganese grades will not show a consistent statistical superiority. However, they are certainly not inferior in crack resistance, which is an important consideration in view of their advantages in yield strength and intrinsic toughness as shown in earlier sections of this paper.

Carbon and phosphorus were included as variables in some of the molybdenum bearing rods. Some of these heats also appear in Table 4, which shows their tensile properties. Carbon does not have a clearly defined influence on bead cracking, but the 0.036% phosphorus level does appear significantly better than 0.045–0.050%. The same phosphorus effect has been confirmed with nickel-manganese electrodes and with heat 51-299 it is established without either nickel or molybdenum. This heat is superior to the others shown and gave complete freedom from bead cracks in 31 out of 32 welds.

It seems likely that this phosphorus effect, operating erratically and unrecognized, may have been responsible for the claims made that other variables such as carbon, molybdenum or copper content have virtues in preventing cracks. It may also explain why austenitic stainless electrodes, which are characteristically lower in phosphorus than the available manganese steel electrodes, are sometimes preferred when freedom from cracking is essential.

The 2% molybdenum heat 50-227 provides a pattern for a desirable electrode composition. It is outstanding in crack resistance, and also has good yield strength. When deposited upon itself it was free of bead cracks and almost free of underbead cracking.

COMPARISONS OF BASE MATERIALS

Table 12 also summarizes underbead cracking in some of the cast base alloys. Heat 51-299, which was tested as weld deposits on eleven base materials, as well as in base metal blocks, is unalloyed, but is quite low in phosphorus. Nickel and molybdenum were omitted intentionally to demonstrate that crack resistance is dependent on neither element. As a base it minimized cracks from the standard nickel-manganese electrodes and when welded upon itself it almost prevented underbead cracks.

Two carbon levels of ordinary manganese steel were included in both rod and base block groups. Since grain size is a known variable in castings, specimens poured both hot and cold were in the latter. There is no clear difference in bead cracking that is related to carbon content of these rods.

Since perhaps 40–50% of the fusion bead is contributed by the base upon which it is deposited, it is reasonable to expect some influence. With R2569 as the electrode, the merit of 0.85% vs. 1.27% carbon in the base is shown with a probability near 95%. Considering the similar pattern hinted by other data and the better tensile ductility after slow cooling afforded by low carbon (Table 3) the conclusion that weld area properties will be better if the base is below 1.0% carbon seems justified.

The base is expected to be particularly

Table 12—Manganese Steel Weld Bead Crack Count Averages (Each Value Is the Average of Three Tests)

Heat No.	Chemical compositions, % C	Mn	Si	Ni	Cr	Mo	P	Standards*
R2569	0.76	14.36	1.14	3.90	0.26	..	0.048	31.3[d], 19.0[e], 29.0[d], 12.7[m], 17.7[g], 13.7[b], 25.3[e], 15.0[g]
R2569	0.76	14.36	1.14	3.90	0.26	..	0.048	25.0[f], 17.0[b], 34.0[i], 2.7[a]
S1445	0.79	13.04	1.00	4.03	0.22	..	0.041	9.0[i], 4.3[e], 11.3[d], 0.7[m], 10.0[g], 9.0[b], 7.7[e] (letters refer to base compositions below.)
Unalloyed manganese steels								
51-299	0.78	14.2	0.93	..	..	..	0.010	0.0 (on S3945 base) 0.0 (on CG51-607 base) 0.33 (on CG51-608 base)
51-607	0.85	11.2	0.57	..	..	..	0.062	34.3 (on S3945 base) 37.3 (on CG51-607 base) 32.3 (on CG51-608 base)
51-608	1.27	11.7	0.56	..	..	..	0.067	45.0 (on S3945 base) 30.0 (on CG51-608 base) 43.3 (on FG51-608 base)
4% nickel-manganese steels								
49-418	0.69	12.68	0.72	3.51	0.07	..	0.049	14.7 (on 50-325 base) 11.0 (on 50-446 base)
50-032	0.75	13.10	0.54	3.65	0.14	..	0.032	7.34 (on 50-325 base) 1.67 (on 50-032 base)
49-501	0.81	12.95	0.93	3.55	0.18	..	0.039	15.0 (on 50-325 base) 6.0 (on 4-903 base)
50-507	0.86	13.30	1.30	3.53	0.08	..	0.036	9.0 (on 50-325 base) 10.3 (on 50-507 base)
1% molybdenum-manganese steels								
49-210	0.61	11.75	0.17	0.23	..	1.05	0.037	0.33 (on 50-455 base)
49-201	0.69	11.8	0.36	(Cu: 1.15)		0.53	0.036	0.57 (on 50-455 base)
49-282	0.83	11.6	0.38	0.19	..	0.96	0.043	3.7 (on 50-455 base)
50-553	0.89	14.1	0.50	..	..	0.99	0.041	38.0 (on 50-455 base)
2% molybdenum-manganese steels								
50-552	0.74	13.9	0.51	..	..	1.92	0.080	45.0 (on 50-455 base)
50-227	0.75	13.9	0.54	..	0.07	2.00	0.033	3.0[g] (on 50-455 base), 0.0[m], 1.0[e], 1.0[e], 2.3[d], 2.0[d] 3.0[b]
50-583	0.76	13.96	0.62	..	..	1.99	0.084	29.7 (on 50-455 base)
50-521	0.77	14.0	0.54	..	..	2.00	0.050	27.7 (on 50-455 base)
50-558	0.79	14.4	0.61	..	..	2.00	0.079	35.0 (on 50-455 base)
50-545	0.90	13.9	0.58	..	..	1.79	0.066	30.7 (on 50-455 base)
50-520	0.90	13.9	0.55	..	..	2.00	0.088	45.7 (on 50-455 base)
50-582	0.91	14.12	0.60	..	..	1.97	0.088	31.3 (on 50-455 base)
50-557	0.93	14.2	0.61	..	..	1.95	0.045	27.0 (on 50-455 base)

Underbead cracks in some of the base metals

									Avg underbead cracks in a 2-in. section
(a)	51-299	0.63	14.2	0.93	..	..	..	0.010	10.0, 2.7
(b)	51-607CG	0.85	11.2	0.57	..	..	..	0.062	15.0, 13.0, 14.3
(c)	51-607FG	0.85	11.2	0.57	..	..	..	0.062	37.7, 11.0
(d)	50-325	1.11	12.92	0.58	..	0.05	..	0.037	6.3, 13.7, 16.7, 13.7, 2.7, 8.0, 10.7, 13.3, 17.3, 14.7
(e)	50-322	1.13	12.76	0.57	..	0.06	..	0.038	4.3, 7.0, 7.3, 15.0
(f)	S3948	1.15	12.55	0.85	..	0.49	0.02	0.059	29.0, 32.3, 37.3, 34.3
(g)	50-455	1.20	12.88	0.55	..	0.02	..	0.048	7.0, 10.7, 16.0, 19.0, 18.0, 12.7, 12.7
(h)	50-464	1.10	12.55	0.62	..	0.04	..	0.038	
(i)	50-608	1.27	11.7	0.56	..	..	..	0.067	Coarse grained: 14.0, 16.3, 18.3; fine grained: 51.7, 75.5
(j)	50-563	0.75	13.0	0.66	3.47	..	..	0.045	10.0
(k)	50-564	0.75	13.2	0.61	3.44	..	..	0.081	22.7
(l)	50-032	0.75	13.10	0.54	3.65	0.14	..	0.032	3.3
(m)	50-446	0.84	12.93	0.57	3.49	0.08	..	0.038	
(n)	50-507	0.86	13.30	1.30	3.53	0.08	..	0.036	6.7
(o)	49-603	0.81	13.2	0.60	3.53	..	..	...	17.3, 12.3, 10.7
(p)	50-370	0.70	14.2	0.99	..	..	1.10	0.025	16.0
(q)	50-227	0.75	13.9	0.54	..	0.07	2.00	0.033	1.7
(r)	50-362	0.80	13.8	1.07	..	..	1.10	0.018	18.7

* At least one of these was included in each lot comparison group.

important in relation to underbead cracking, since the cracks reflect stress relief of the heat-affected zone during cooling from welding heat. Lot 51-299 shows a 90–95% probable superiority to two of the nickel-manganese bases in underbead crack resistance and would likely be more clearly superior in other cases were it not for the bad scatter in the higher crack counts. The fine-grained 1.27% carbon composition (51-608) shows a significantly greater number of cracks than its coarse-grained companion. This should not be interpreted as inferiority; it probably only means that fine-grained steel presents more grain boundaries that can give way under welding stresses. Grain size is a variable that can seriously confuse underbead crack counts.

The practical significance of the underbead cracking that has been shown here to be quite prevalent is not always clear. The prompt peening of the red hot weld bead that is generally recommended for manganese steel should minimize them as well as bead cracks. (None of these test welds were peened.) On the other hand, even one crack presumably could lead to premature failure of a critically stressed part. Were it not for the inherent toughness of austenitic manganese steel, and its amazing ability to resist crack propagation the prevalence of bead and underbead cracks would probably have been more generally recognized.

Tension is more likely to reveal the adverse effects of cracks than is compression. Most manganese parts are stressed in compression on the wearing face. Test bars are an exception and cracks help explain why the properties of welded specimens are below those of cast bars even where the latter have been slowly cooled, as detailed in the next section.

By combining the benefits of alloying, low carbon and low phosphorus, it has been possible to make welds by the procedures described here that are completely free from bead and underbead cracks, without peening. Since the conditions are severe, such combinations offer promise of solving the problems of those who need even tougher manganese steel weldments.

WELD DEPOSIT PROPERTIES

There is some likelihood that a reader of the foregoing details will conclude that so many adverse factors tend to degrade the properties of manganese weldments that welding should be avoided. While perhaps valid, this conclusion is not practical. Experience has proved that such weldments serve many purposes and a considerable tonnage of these electrodes is used every year with satisfactory results.

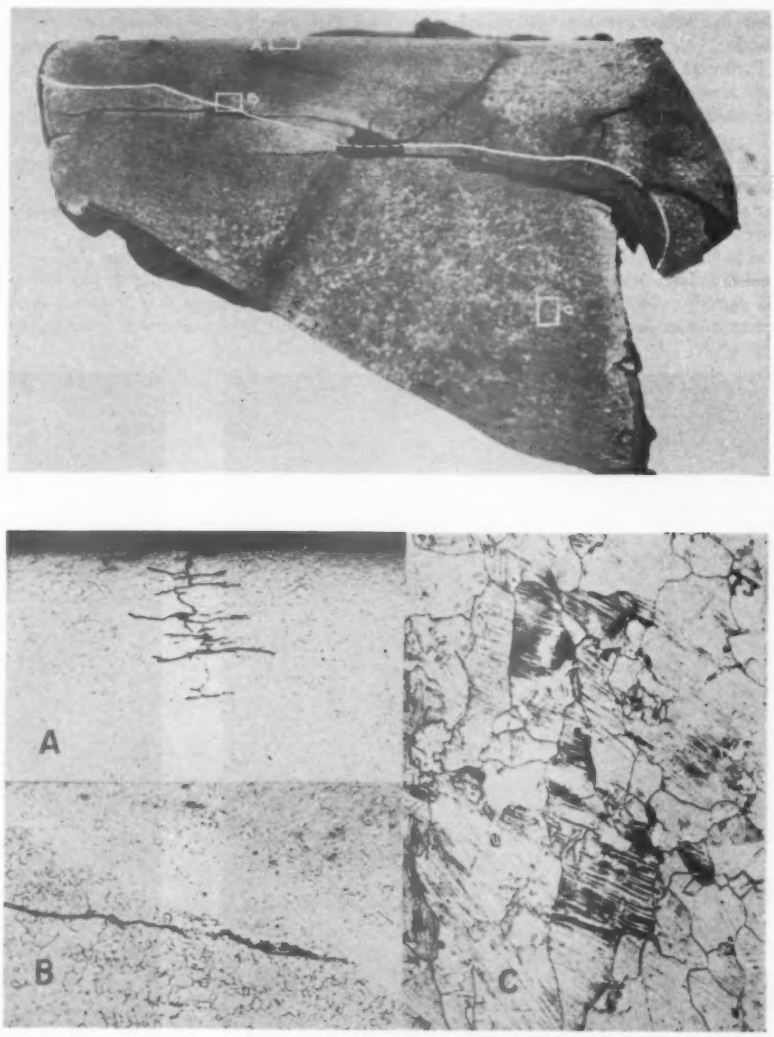
Those who have used manganese steel electrodes have reached various conclusions about the adverse factors, as in the several patent disclosures. Danhier[15] recently featured the effect of phosphorus on cracking (confirming the quantitative data in this paper) in contrast to the earlier reported effects of carbide precipitation and transformation embrittlement. There seems to be a strong tendency to blame or credit a single factor, as in the patents. Danhier[15] also, for example, states that he has never found any indication of carbide precipitation in the heat-affected zone of steels containing less than 1.1% carbon,* and attributes poor properties principally to the underbead microfissures.

* Carbide precipitation has been observed by the authors even in low carbon manganese steels, but it is less serious. However, nearly all commercial wrought or cast manganese steel carries more than 1.1% carbon.

The fact that any one of the factors can dominate, but that they are likely to operate in combination, deserves emphasis. One purpose of this paper is to present a balanced story that will lead to improved weldments.

There is also a tendency to forget properties important from the service viewpoint and perhaps to depend too heavily on such conventional values as ultimate tensile strength and elongation. Yield strength and work hardening capacity are generally more important and may not be seriously affected by the usual adverse factors.

It will be helpful to examine several weldments in detail. Figure 10 is a cross section of a welded manganese steel frog



Courtesy of C. J. McConaughy, Southern Pacific Co.

Fig. 10 Cross sections of a failed trackwork weld (magnification: 7½ ×; 100 × for detailed photomicrographs); weld junction is traced in white to show that the main shear failure occurred chiefly in the base metal (reduced 20% in reproduction)

(*A*) Microscopic shear cracks under the running surface. (*B*) Crack in base metal that has been reaustenitized by the heat of welding. The crack here stops when it reaches tough weld metal. (*C*) Slip bands in the base metal that are evidence of work hardening before the weld was made. This structure is especially likely to be embrittled by reheating.

that has failed in service. Much plastic flow occurred, with shear failures on a gross as well as a microscopic scale. Over half of the main fracture plane is in base metal. It intersects two defects that appear to be areas of incomplete fusion.

The various cracks in the weld metal suggest that fissures were present after welding, but they do not appear responsible for the main failure. It probably started at the voids on the weld junction and propagated chiefly through the heat-affected underbead zone a short distance from the junction. Carbide precipitation characteristically causes some loss of toughness here. Further, below the junction more severe embrittlement is in evidence microscopically, but service stresses were apparently too low to cause cracking. Near the large defect in the center of the specimen a base metal crack that stopped when it reached tougher weld metal was found.

Figure 11 shows an Auto-Arc weld (no flux used) of bare $^{5}/_{32}$-in. diam 0.47% carbon, 2% molybdenum-manganese steel on a standard cast manganese steel base using 24 v, and 2.7 kw arc power, reversed polarity and a traverse speed of 4.0 in. per minute. It has been etched to show the dendritic pattern of carbon segregation in both weld and base metals. The weld metal is sound and free of precipitated carbide. The base shows underbead cracks and grain boundaries accentuated by carbide precipitation. (Near the lower left some shrinkage porosity occurs.)

Cracks such as these are not considered to be serious by themselves. They ordinarily will progress only among embrittled grain boundaries, which places a premium on toughness of the base metal. Even with the amount of grain boundary carbide shown no trouble is expected unless gross tensile or shear stresses above the yield strength are imposed, which is unusual for the overlay and surfacing applications of manganese steel.

Tensile test conditions are a different matter. Table 13 includes data from both butt welds and all-weld-metal specimens. Figure 12 is a section from a weld with bare 0.52% carbon, 1.70% molybdenum electrodes manually welded against rolled manganese steel plate 1 in. thick. The weld metal was intended for tensile testing but was found to contain too many voids for this. Its multiple pass make-up adapted it to sampling for temperature effects, which are shown in the insets.

The seriously heat-affected zone can be seen at low magnification and is magnified 500 × in the two lower insets. Close to the junction both acicular and grain boundary carbides are prominent, indicating serious embrittlement.

Within the weld deposit, and especially near the bottom, carbide precipitation along grain boundaries is evident, contrasting with relatively clean grain boundaries near the top of the deposit. Each successive pass has contributed its reheating effect to those made before, and some carbide has been released from solution despite the original relatively low value. Only the upper layers, which have had the least reheating, are relatively free from grain boundary carbide.

Thus it may be seen that welding heat does affect even the low carbon, alloyed austenite of the deposited metal, though the effect on the higher carbon base metal is greater. This argues for keeping temperature as low as possible. However, beyond a certain point incomplete fusion will result as shown at the edge of Fig. 13 and in occasional areas of Fig. 12. In Fig. 13 both difficulties are shown to occur at the same time. With cast manganese steel embrittlement is usually less than in Fig. 13. The least damage to the base occurs when it is similar to the electrode compositions. Bars, rounds and flats of this type are commercially available and promise tougher assemblies than are usual with ordinary manganese steel filler bars and the like.

AUTOMATIC WELDING

Automatic welding, while having wide industrial utility, has several unfortunate limitations. It usually is done under a blanket of granulated flux, which prevents observation of the welding arc and the de-

Fig. 11 Bare electrode automatic arc-weld cross section showing grain boundary carbide precipitation in the base metal and underbead cracks (magnification: 25×; insets 500 ×) (reduced 50% in reproduction)

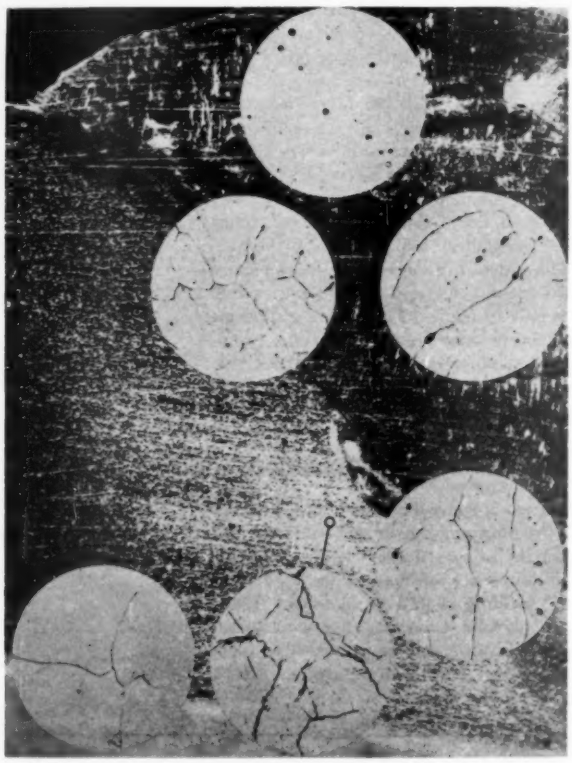


Fig. 12 Structures developed in and near a multilayer weld with 0.52% C, 1.70% Mo manganese steel on a rolled manganese steel plate ($^{5}/_{16}$-in. bare electrode) (magnification: $7^{3}/_{4}$ ×; insets 500 ×) (reduced 50% in reproduction)

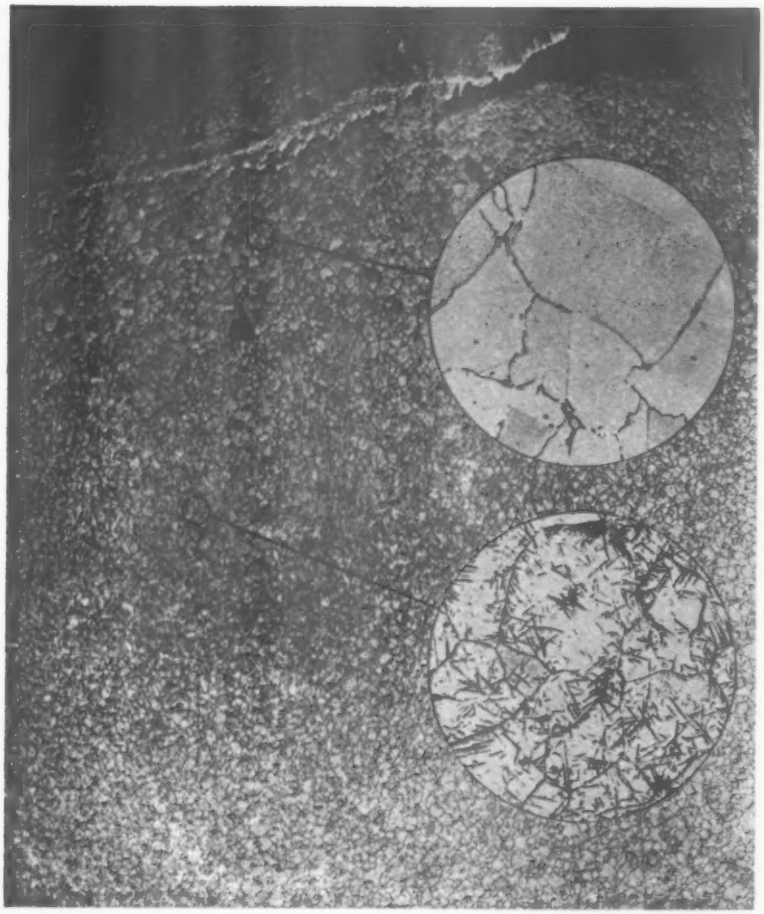


Fig. 13 Poor edge bonding, underbead tears and reheating embrittlement from an arc weld (27 v, 4.0 kw) with bare 3/16-in. nickel-manganese electrodes on a wrought manganese steel plate (magnification: 20 ×; insets 250 ×)

posited bead. The flux must be fed to the arc area by special equipment and must be removed after welding. Directly over the bead it forms a fused slag, which requires crushing if it is to be reclaimed.

Because the nickel and molybdenum manganese steels can be used satisfactorily as bare electrodes, they provide a unique advantage for automatic welding. Figure 14*A* is the surface and Fig. 14*B* is the cross section of a bare wire automatic deposit, using an Auto-Arc welding head. The deposit is comparable in quality to a manual weld with flux-coated electrodes, but offers a lower cost method of surfacing large areas.

The steady heat input from automatic welding will damage a manganese steel base unless auxiliary cooling is provided. The base under the weld in Figs. 11 and 14 was cooled with water which minimized damage to the underbead metal. The heat input from one side will also lead to stresses that tend to produce distortion. Unless restrained the weld bead side of the specimen like that of Fig. 14 will become concave after cooling. Control of this distortion becomes an individual problem for each job. Good welders are likely to have a number of ways to deal with it.

The weldment shown in Fig. 11 represents automatic bare arc deposition (5/32-in. rod, generator set for 150 amp, reverse polarity 24 v, 2.7 kw arc power).

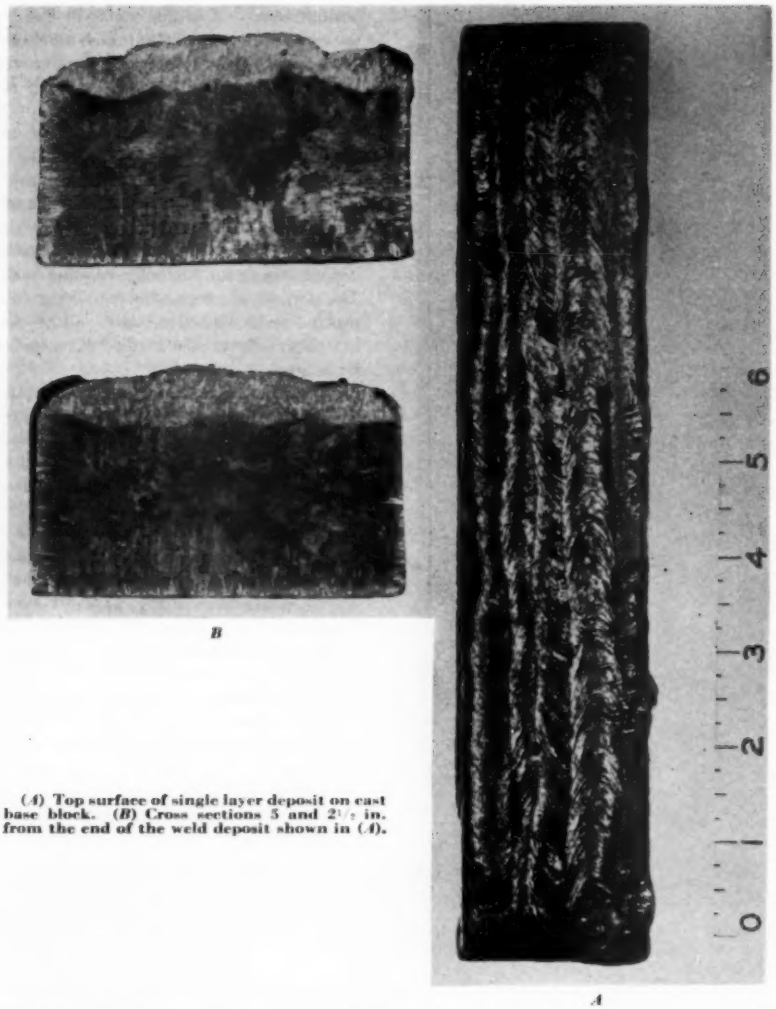
The wide latitude in arc power that is associated with automatic welding of mild steel is apparently not available with manganese steel. To little power may lead to incomplete fusion, a narrow, high bead being characteristic. Too much

Table 13—Tensile Test Properties of Manganese Steel Arc Welds

Manual welding with bare electrodes

Heat No.	*Composition, %* C	Mn	Si	Ni	Cr	Mo		*Test No.*	*Yield strength psi*	*Ultimate tensile strength, %*	*Elongation in 2 in. %*	*Reduction of area, %*	*Hardness, Bhn*
									53,490	65,160	11.5	16.6	...
									56,100	70,540	5.0	7.7	...
									56,110	68,860	10.0	13.3	...
									58,930	72,670	5.0	15.7	...
	Butt welds, joining cast and heat-treated plates with nickel-manganese electrodes								42,350	42,350	nil	..	...
									56,300	64,010	6.0	14.9	...
										97,000	12.4	16.6	...
										81,750	10.3	10.3	...
									52,920	60,690	8.0	18.9	...
									53,130	76,640	11.5	12.3	...
	Butt welds joining manganese steel plates; electrode composition												
r	0.66	12.45	0.20	tr	0.07	0.18		J774A	79,400a	98,000	11.8	15.7	...
r	0.66	12.45	0.20	tr	0.07	0.18		J774B	73,900a	79,400	8.4	12.7	...
r	0.62	13.23	0.74	3.97	0.07	0.04		J786A	87,400a	97,000	12.4	16.6	...
r	0.62	13.23	0.74	3.97	0.07	0.04		J786B	73,400a	81,750	10.3	10.3	...
r	0.67	14.55	1.11	tr	0.07	1.04		J798A	71,100a	81,600	15.4	11.0	...
r	0.67	14.55	1.11	tr	0.07	1.04		J798B	62,600a	63,800	12.5	5.9	...
r	0.81	15.02	0.62	..	0.23	0.02	(135 amp)	U470	63,500a	79,000	8.5	11.5	187
	All-weld-metal specimens												
51-299d	0.63	12.80	0.63	..	..	..	(160 amp)	U2000	60,800e	82,200	10.0	17.4	223
51-299d	0.63	13.03	0.66	..	..	..	(150 amp)	U2415	57,200a	84,400	13.0	10.4	217
W872r	0.75	14.04	0.55	0.10	0.04	1.98	(140 amp)	W1557	70,800e	106,500	18.0	20.2	196
W872r	0.75	14.04	0.55	0.10	0.04	1.98	(160 amp)	W2302	67,500e	112,500	22.0	20.2	217

NOTES: "e" by yield strength indicates a precise 0.2% set value from an extensometer. "a" by yield strength indicates that it obtained by dial arrest method and is not precise. "d" by heat number indicates chemical analysis of a weld deposit (see Table 12 electrode heat analysis). "r" indicates a chemical analysis of rod samples.



(*A*) Top surface of single layer deposit on cast base block. (*B*) Cross sections 5 and 2½ in. from the end of the weld deposit shown in (*A*).

Fig. 14 Multiple bead automatic deposit of austenitic 14% manganese steel (0.78% C, 0.25% Cr, otherwise unalloyed) on standard cast manganese steel. The deposit was open arc welded (no flux blanket) by an inexperienced operator, using Auto-Arc equipment, 3/16-in. diam coiled bare wire, and 4.2 kw arc power (reversed polarity, 200-amp generator setting). Note that the deposit is sound despite the absence of arc shielding

Procedure Notes for Welding Manganese Steel

Manganese steel is very tough, but it is difficult to retain this toughness after welding. Experience and research have shown that the following procedures are helpful.

1. Manganese steel weld metal should not be mixed with carbon or low alloy steels. The result is brittle metal that is likely to contain serious cracks. Electrodes containing from 0.060 to 0.90% carbon, over 13% manganese and either 3–4% nickel or 1–2% molybdenum should be used for welding on manganese steel. If manganese steel is to be overlaid on carbon steel, separate them with a layer of austenitic stainless steel like Type 310 (25% Cr:20% Ni).
2. Manganese steel can be welded with austenitic stainless electrodes, but their use should be avoided if any flame cutting may be done later since they resist it very effectively. They are also comparatively expensive. Nickel or molybdenum manganese steel can be cut readily with an oxy-acetylene torch.
3. Oxy-acetylene welding of manganese steel should be avoided. It is likely to cause marked embrittlement of the base metal.
4. The lowest possible arc power should be employed. The limit is imposed by arc instability and incomplete fusion bonding. For critical jobs experiment with scrap metal before proceeding.
5. Generators supplying a high open-circuit voltage are preferred for ease in arc striking.
6. Reversed polarity direct current (the work should be negative) is preferred for both coated and bare rods.
7. Small weld beads should be avoided. A bead with a large cross section is stronger and resists cracking better. This is particularly important for the root pass of butt welds.
8. Deposition with a weaving, crescent motion is helpful in avoiding slag inclusions.
9. Immediate peening of the red hot bead will minimize cracking, which is more common than is generally realized. Peening will also reduce warping.

power gives a wide flat bead with much mixing with the base; porosity is likely, while spatter and base metal heating are excessive. Around 175 amp for 3/16-in. wire seems near the optimum range for a bare electrode and no flux. Some of the economy of automatic welding relates to high deposition rates attained by high arc power. This can be expected to produce cracks as it does with manual welding. A flux blanket will permit making a smoother, straighter bead, but it will not eliminate the cracking problem and causes greater heat build-up in the base. The use of manganese steel filler bars welded in place is better for efficient metal placement than high burn-off rates.

WELDING DETAILS

This paper includes a review of the metallurgical background for welding manganese steel and as such replaces an earlier bulletin that is out of print.[16] Literature reviews on this subject were also published[17, 18] some years ago by the Welding Research Council. From all of these it is possible to compile a list of precautions and details that will aid practical welders. Most of these details have evolved from years of experience, but the effects of arc power and bead size on cracking are new and worthy of careful consideration.

SUMMARY

This paper has summarized the factors that influence the quality of 13% manganese steel weldments. It points out:

1. The valuable properties of austenitic manganese steel.
2. The virtue of carbon at levels below the standard cast and wrought specifications for maintaining toughness where slow cooling from welding temperatures rather than water quenching is involved.
3. The value of proper alloying to maintain yield strength and toughness.
4. The harmful changes that can occur in the heat-affected zone.
5. The main causes of bead cracking, which are (*a*) excessive arc power, (*b*) insufficient bead size and (*c*) excessive phosphorus content.
6. The conditions under which automatic arc welding without a flux blanket are feasible.
7. The need for care and understanding if the factors that degrade the toughness of manganese stee are to be held in check.
8. A number of practical procedure hints that will aid in obtaining good weldments

ACKNOWLEDGMENT

The authors are indebted to many individuals with years of cumulative experi-

10. Welding should be done with as little heating of the base metal as possible. Intermittent welding, water cooling of the base and the use of temperature indicating crayons (Tempilstiks) are helpful.

11. If several jobs can be done at the same time the deposition of an electrode on each in turn will permit better cooling. With only one job, skip welding is advisable.

12. When a temperature indicator is available the zone $\frac{1}{2}$ in. from the weld should not be allowed to exceed 500° F. It should be possible to hold a bare hand on the metal 6 in. away.

13. Any defective areas (sand inclusions, shrinkage porosity, slag, cracks and the like) should be removed before welding.

14. In crack repair the entire crack should be burned or ground* out before welding and the end of the channel terminated with a hole or a generous fillet. This step requires care, since the end of the crack may be difficult to find.

15. Wherever possible, work-hardened manganese steel should be removed before welding, as it is more susceptible to embrittlement than in the soft condition. Areas that cannot be easily indented with a center punch should be ground out.

16. For abrasive conditions an overlay of a martensitic iron on manganese steel is expected to improve wear resistance.

* Grinding is advisable with trackwork.

ence in the American Manganese Steel Division for many valuable observations and procedure details. W. A. George has been especially helpful with practical welding knowledge, H. D. Sweeney and E. L. Wegerson with field observations and H. A. Fabert and C. E. Ridenour with metallurgical assistance.

Consultation with R. H. Schaefer and J. A. Fellows of the American Brake Shoe Co. Research Center has also been very helpful. The welding skill of H. C. Satterlee has been invaluable during the test program. N. A. Matthews and R. C. Carruthers have helped by a careful review and criticism of the preliminary manuscript.

Permission from Gilfry Ward, President of the American Manganese Steel Division, to publish this material, and his helpful suggestions are also appreciated.

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Discussion by T. E. Norman

The authors' paper provides us with a very substantial contribution to the metallurgy of austenitic manganese steel. We are particularly interested in the possible application of their findings to our own operations at Climax, Colo., where we use a considerable tonnage of this material in crusher liners. In these liners we have indications that some of the apparent wear is actually flow of metal away from the zones where the crushing stresses are most severe. The low yield strength of manganese steel is obviously responsible for this. Undoubtedly this tendency to flow must also be considered in other wearing parts, including those which are rebuilt by manganese steel welding rod. The higher yield strength of the molybdenum-manganese weldrod compositions studied by the authors should, therefore, contribute real and practical benefits especially where weld deposits are to be used to resist high compressive or tensile stresses.

Another very interesting observation reported by the authors is that work-hardened manganese steel has no better resistance to severe abrasive wear than the same composition without prior work hardening. This confirms our own observations where we studied the wear of manganese steel grinding balls under varying degrees of impact. We were surprised to find that with increasing degrees of impact, in a 3-ft-diam test mill, the austenitic manganese balls actually wore faster and also had a higher abrasion factor, when compared to high carbon martensitic steel as a standard.

If prior work hardening of manganese steel will not contribute to its wear resistance, it then appears desirable to investigate other means of prolonging the life of manganese steel wearing parts. As has been pointed out by the authors, one method of doing this is to apply hard-facing overlays. Recently some users of crusher liners have also been fairly successful in incorporating hard-facing alloys as filler materials in austenitic manganese steel built-up deposits. In either method, it seems most desirable to apply these deposits to a base composition which will not be embrittled easily by the heat from the weld deposits. The authors' studies have clearly indicated some of the steps which should be taken to avoid or reduce this embrittlement. They deserve our sincere commendation for a thorough job well done.

T. E. Norman is Metallurgical Engineer of the Climax Molybdenum Co., Denver, Colo.

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Diesel Engine Valves Reclaimed by Sprayweld Process

by L. V. La Rou

DIESEL engine valves reclaimed by applying the Colmonoy Sprayweld process to coat worn seating surfaces with an impact and wear-resistant nickel-base alloy are outwearing new alloy steel valves in actual running tests in 800-hp Diesel freight engines on a southern railroad line.

The coated valves, Fig. 1, showed practically no wear after 355 days operation in the railroad Diesel engine and were still serviceable. New alloy steel valves installed in the engine at the same time as the hard-faced valves, showed 1/4 in. wear on the seating surfaces and were completely worn out. The 355-day period is the equivalent of about 4000 hr of continuous operation. The valves have a 4 in. diam and are 13 in. long.

L. V. La Rou is Chief Engineer at Wall Colmonoy Corporation.

Fig. 1 A comparison of new and Spraywelded Diesel engine valves after 4000-hr operation in the same freight engine on a southern railroad line

The new alloy steel valve (left) has worn 1/4 in. and is completely worn out. The Colmonoy-coated reclaimed valve (center) shows practically no wear and is still serviceable. The valve at the right, which has been reclaimed by Spraywelding and grinding, shows the appearance of the center valve before installation.

Fig. 2 Applying the alloy coating by rotating the valve in front of a tool post mounted Spraywelder pistol

The alloy, is applied to the worn valves by the three-step Sprayweld process at a cost of from one-third to one-half the price of a new valve.

METHOD OF APPLYING WEARPROOF ALLOY

In this process the worn valve seating area is first threaded along the tapered surface or gritblasted to provide the necessary mechanical bond for the subsequent metal spraying operation.

Then the valve is chucked in an engine lathe or turning fixture and rotated, Fig. 2, while the powdered alloy is applied by a Spraywelder pistol supported in a tool post holder. This pistol is connected through hoses to a wall or pedestal-mounted panel unit that supports a powder hopper, carburetor, air regulator, air filter and connections to oxy-acetylene tank and factory air supply lines.

Fig. 3 Fusing the sprayed-on nickel base alloy coating by rotating the valve under an oxy-acetylene flame

After the alloy has been sprayed on the valve seating surface to a thickness that will provide a finish ground alloy thickness of 0.060 in., the pistol is swung away from the chucked valve.

In the final step, the valve is rotated under an oxy-acetylene torch, Fig. 3, to fuse the nickel-base alloy coating to the base metal, thus providing a fusion bonded, non porous coating of wear- and impact-resistant alloy.

The seating area is finished ground on a conventional external grinding machine before installation in an engine.

NICKEL-BASE ALLOY CHARACTERISTICS

The alloy applied to the Diesel engine valves is a nickel base alloy containing chromium and boron. It is nonmagnetic and has a Rockwell C hardness from 45 to 50 and melts at 1950° F. The alloy has excellent impact, and corrosion resistance as well as excellent red hardness. Its abrasion and galling resistant qualities are very good. It can be had in cast shapes as well as in powder and bare and coated welding rod form.

Kelcaloy Transition Sections

HERE, ready for a step in the complicated heat-treating required during their fabrication, are six out of a total order for 50 unique, bimetallic Kelcaloy transition pieces manufacture by the M. W. Kellogg Co. for use in England on ultra high-temperature, high-pressure steam systems.

They will be used to connect ferritic throttle valves with austenitic steel main steam piping.* The pieces will be installed by Aiton & Co. Ltd.

First use of such transition pieces was made by the Public Service Electric and Gas Co. of N. J. on four units in its Sewaren Station, which operates at 1050 F and 1500 psig. The main steam piping at Hudson Avenue Station Annex of Consolidated Edison Co. of New York,

* These are for units in England where the design practice is the reverse of that in the United States. Chrome-molybdenum is used for the valve and hook-up to the steam chest, while stainless, because it may be designed for thinner walls, is used for increased flexibility in the main steam piping.

operating under similar conditions, is also equipped with Kelcaloy transition pieces. In both these instances, the pieces were used to connect chrome-molybdenum main steam piping with stainless steel throttle valves, contrary to the British practice where stainless steel piping and ferritic valves are used.

The basic reason for the use of transition pieces lies in designer's reluctance to join by a butt weld two metals of widely varying thermal coefficients of expansion. The ferritic 2¼% chrome – 1% molybdenum and the austenitic Type 347 steels have a difference of approximately 50% in their thermal expansion coefficients.

The transition piece eliminates the need for welding these different materials together. This is accomplished by Kellogg's patented Kelcaloy process in which austenitic steel is integrally bonded to a ferritic chrome-molybdenum forging to provide a transition piece which is of the desired chrome-molybdenum steel at one end, and austenitic stainless at the other, and having a central transition bond of controlled axial length.

Following casting, the transition pieces are machined and bored to proper dimensions and heat treated. During these steps, they are inspected by such non-destructive test methods as liquid penetrant magnetic particle, ultrasonic and radiography.

Flash Welding Tubing to Forgings

IN FABRICATING components of large aircraft landing gears, The Cleveland Pneumatic Tool Co. makes numerous parts by flash butt welding lengths of steel tubing to mating drop forgings. For quality control of such welded parts, the company proof-tests all of the welds.

The size of such parts has been increased and the ease of testing large parts has been improved by recent installation of a 1,000,000-lb hydraulic testing machine built by Baldwin-Lima-Hamilton Corp. Fash butt welds with cross-sectional areas up to 67 sq in. and having diameters up to 30 in. can be proof-tested in this floor-type testing machine.

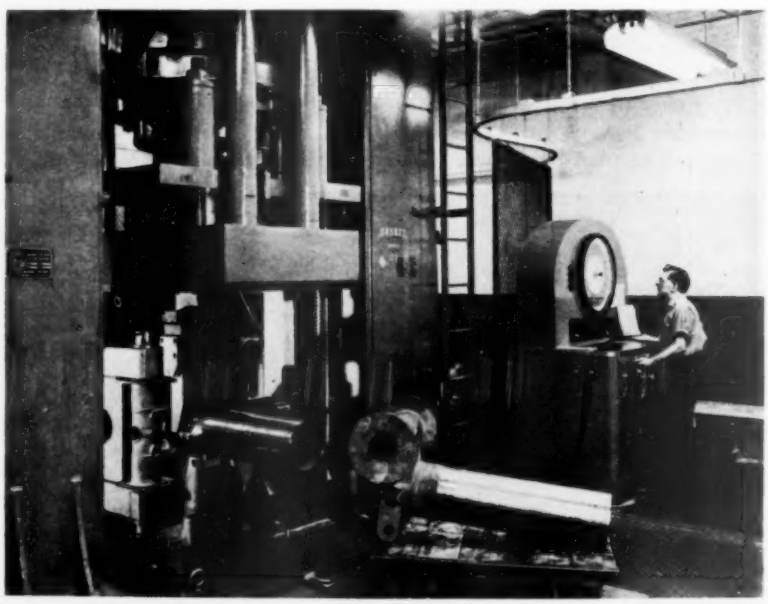
Flash-butt-welded parts are given bending tests in four positions, using specially designed adjustable fixtures to hold the parts. For production testing, the fixture is used in one position for a series of tests of a group of parts, then it is re-set for the succeeding test. The landing gear piston being tested in the illustration is approximately 10⅓ in. outside diameter with ½-in. wall. The test is made after the excess flash has been removed and after heat treating, under loads up to 80% of the yield strength of the material.

Production testing of welds in landing gear pistons is fully justified by the advantages resulting from the use of flash butt welding in manufacture instead of making a one-piece forging. Not only is there a considerable saving in the size of the drop forging required and the amount of machine work necessary to fabricate the part, but the welding process permits use of smaller forging machines and dies. This permits the procurement of forgings from a larger number of sources of supply.

The new testing machine is located adjacent to the machine shop where the parts are finish machined after tests. Only the lower part of the 28-ft high machine can be seen in the illustration. For tensional tests landing gear parts are handled on the next floor above.

The testing machine is a standard Baldwin-Lima-Hamilton machine of the type that carries all loading and load measuring equipment on the sensitive crosshead and has no machinery under the floor. Special accessories include automatic controls on the Baldwin-Tate-Emery control and indicator cabinet to release the load when it reaches a predetermined maximum or to hold the load for predetermined lengths of time. A foot switch connected into the control cabinet permits the operator to hold the gate into the test area closed during the time load is applied. The operator is protected by two heavy screens that roll into position around the control cabinet on an overhead track.

A clearance of 6 ft between columns gives all of the space required for test specimens and the fixtures in which they are mounted. Maximum vertical space for compression tests is 15½ ft and for tension tests is 14 ft. Loads can be applied up to 3½ ipm. In addition to the 1,000,000-lb load range the indicator has three other scale ranges: 0–200,000, 0–50,000 and 0–10,000 lb.



Large aircraft landing gear parts can be subjected here to loads up to 1,000,000 lb for proof testing flash butt welds joining the cylindrical portion and the forged section of a landing gear part

Officers Nominated by Nominating Committee

President-Elect

Joseph H. Humberstone

Mr. Humberstone, Vice-President of Air Reduction Co., Inc., was born on Oct. 25, 1909, in Toledo, Ohio. In the year 1931 he obtained a degree of Bachelor of Metallurgical Engineering from Ohio State University. Immediately upon graduation, he was hired by General Electric as a Development Engineer and concerned himself with development activity on coatings as applied to heavy covered metal-arc welding electrodes. He was in charge of such development activities during most of his years with General Electric.

In January 1939, when the Arcrods Corp. was formed, he was transferred and appointed Welding Engineer of that firm. In this position he dealt specifically with applications of metal arc welding involving heavy covered electrodes.

During World War II Mr. Humberstone, on leave of absence from Arcrods, joined the War Metallurgy Committee of the National Research Council in the capacity of Research Supervisor of Welding. After handling this assignment during the period of October 1942 to March 1944, he returned to the Arcrods Corp. and assumed the duties of Director of Research and Welding Engineering. He became successively Vice-President, President and Chairman of the Board.

Toward the end of the year 1948, Mr. Humberstone became associated with Air Reduction as Manager of Apparatus Production for the Air Reduction Sales Co. When, in January 1950, Air Reduction Co., Inc., instituted a program of decentralization, Mr. Humberstone became President of the Airco Equipment Manufacturing Division. Late in 1951 he was appointed Vice-President of Air Reduction Co., Inc., and relinquished his position as President of Airco Equipment Manufacturing Division. As Vice-President of the parent organization he was given a new assignment in February 1953 as President and General Manager of Ohio Chemical & Surgical Equipment Co., another Division of Airco, which is engaged in the manufacture and sale of medical gases and surgical equipment. He continues in both of these capacities with Air Reduction Co., Inc., and is still associated with Arcrods Corp. as Chairman of the Board.

Mr. Humberstone has been a member of the SOCIETY for over twenty years. He has served the AWS in many capacities—as a member of committees dealing with special problems requiring the attention of the Board of Directors as well as the technical affairs of the SOCIETY. He is probably best known to most of us for his years of service as a member and Chairman of the AWS-ASTM Filler Metal Committee. He was a District Vice-President, Director-at-Large and presently is First Vice-President of the SOCIETY.

Mr. Humberstone is the author of many technical papers on metal-arc welding electrodes and holds several patents on coating formulas and production methods in that field.

1st Vice-President Elect

J. J. Chyle

John J. Chyle

John J. Chyle graduated in Chemical Engineering from the University of Wisconsin in 1924. After working for one year at Smith Steel Foundry, Milwaukee, he joined the A. O. Smith Corp. as a chemist in the Production Laboratory. In 1926 he was named head of Welding Electrode Research at A. O. Smith, and in 1937 he was appointed Director of Welding Research.

Mr. Chyle has invented and patented a number of welding electrodes and welding processes. The first extruded, all-position type of electrode was developed by him in 1927. This original electrode was of the cellulosic titanium dioxide type, which is now classified as the E6010 type.

Special alloy electrodes for the welding of high-strength alloy steels were also developed by Mr. Chyle and used extensively during World War II on aircraft landing gears, torpedo airflasks and bombs. Welding processes originated under his direction which included those for the welding of low-alloy high-tensile steels.

Mr. Chyle is active in technical committees and in technical societies, including the AMERICAN WELDING SOCIETY, American Society for Metals, American Society for Testing Metals and the American Society of Mechanical Engineers. He

helped to organize the Milwaukee Section of the AMERICAN WELDING SOCIETY in 1932 and was Secretary and Treasurer for about six years. In 1939 he was elected Vice-Chairman of the Milwaukee Section and was Chairman in 1940 and 1941.

Mr. Chyle also served on many other committees, such as the Chairman of the Automotive Committee, a member of the Technical Activities Committee of the AMERICAN WELDING SOCIETY, plus many other committees of the AMERICAN WELDING SOCIETY. He also assisted in the preparation of many chapters of the 1950 edition of the WELDING HANDBOOK.

He was a director of the AMERICAN WELDING SOCIETY, elected for a period of three years, 1948–51.

Some of his most recent talks were on "Practical Aspects of Welding Titanium Alloys" and "Factors in the Selection of Welding Processes; he also gave the 1951 Adams Lecture which was entitled "The Welding of Copper by the Inert-Gas Metal-Arc Process."

Mr. Chyle was recently elected as the 2nd Vice-President of the AMERICAN WELDING SOCIETY for a term of one year.

2nd Vice-President Elect

C. P. Sander

Clarence P. Sander is General Superintendent, Vernon Plant, of Consolidated Western Steel Division of United States Steel Co. at Los Angeles, Calif.

Mr. Sander was formerly with the Pacific Car & Foundry Co., Seattle, Wash., and Portland, Ore., in the Engineering Department, and Chief Estimator and later Assistant General Manager for many years, handling labor problems and Production for both plants.

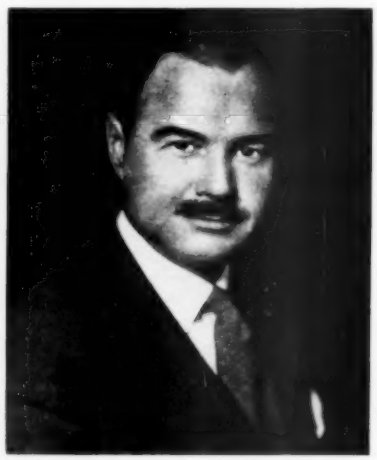
He joined the Western Pipe and Steel Co. in 1934 as General Superintendent of Shop and Field-Operations, Southern Division, until it became a part of the Consolidated Western Steel.

Mr. Sander is a graduate of Wilson's College, Seattle, Wash., with postgraduate work in engineering at the University of Washington and the University of Southern California.

He has been a member of AWS for 20 years, Sustaining Member since 1937; Past-Chairman of Los Angeles Section AWS; Vice-President Western District AWS 1946–47; Member of Welding Research Council, AWS; American Society of Metals and API, besides various social and civic clubs.

His experience covers a broad field in welding such as railroad equipment, shipbuilding, pipe line, boilers, penstocks, pressure vessels, oil refinery installations, steel structures, tank and various other welded products.

Directors at Large



J. Lyell Wilson

J. Lyell Wilson, Consulting Naval Architect and Engineer, was born May 9, 1894, in Elizabeth, N. J. He graduated from Webb Institute of Naval Architecture 1914 and is now a member of its Board of Trustees and its Educational Committee. He took postgraduate courses at Brooklyn Polytechnic Institute. His early training includes positions as shipfitter and design draftsman in various shipyards and the U. S. Navy Department. He became Chief Engineer of Talbot Boiler and Engine Corp.

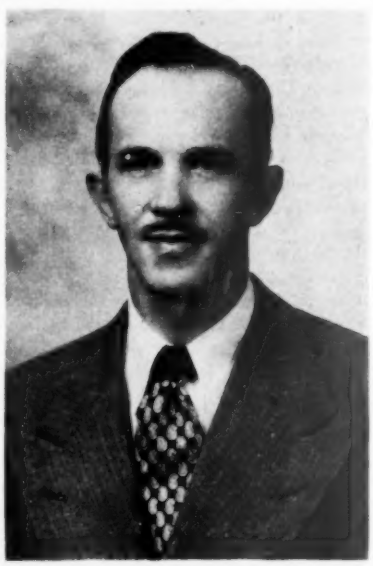
A good part of his professional career was his affiliation with the American Bureau of Shipping. He started as a Surveyor in 1917 and became Assistant Chief Surveyor in 1927. From 1947 to 1950, he held the title of Research Director. He left the Bureau in 1950 and started a private consulting practice. He is a licensed professional engineer in the states of New York and New Jersey.

Mr. Wilson has been very active in the AMERICAN WELDING SOCIETY and is a past-chairman of the New York Section of the SOCIETY. He is currently Chairman of the Technical Activities Committee of the SOCIETY. In the past, he has served as Chairman of the Reserve Funds Committee of the SOCIETY and the committee on Welding in Marine Construction. He is a member of many of the Technical Committees of the SOCIETY including Definitions and Chart, Filler Metal, Methods of Inspection, Standard Qualification Procedures and Metallizing. He has also presented a number of papers before the AMERICAN WELDING SOCIETY and the Society of Naval Architects and Marine Engineers. Of the latter Society, he is past-chairman of the New York Metropolitan Section. He is an active member of ASTM and NFPA.

He has had special assignments including Special Assistant to the Chief of Ships during the fiscal year 1941 and a member of the Sub-Group on the Board to Investigate the Design and Construction of Welded Merchant Vessels, appointed by the Secretary of the Navy. He has assisted in the publication of a number of books, mainly in the Marine field.

E. Donald Peters

E. Donald Peters was born in Cleveland, Ohio, Feb. 7, 1916. He graduated from Case Institute of Technology in 1939 and joined the Cleveland Electric Illuminating Co. as Junior Engineer in the Construction Cost Department. From 1943 to 1946, he was connected with the U. S. Naval Reserve as Ordnance and Gunnery Officer.



E. Donald Peters

He left the Navy in 1946 and became Engineer of the Colonial Iron Works in Cleveland in Estimating and Design work. In 1950, he joined the Leader Iron Works, Inc., Decatur, Ill., as Executive Vice-President and Director of Sales. He became President and Treasurer of that company in 1952. Mr. Peters has been active in the affairs of the SOCIETY including vice-chairman and chairmanship of the Sangamon Valley Section. He was appointed Director of the Association of Commerce of Decatur in 1953. He has also been active in the Boy Scouts of America and is Vice-President of the Macon County District, Lincoln Trails Council.

Ross P. Yarrow

Ross P. Yarrow has been a resident of the city of Lyndhurst, Ohio, for the past 17 years and has been Councilman for 12 years. Mr. Yarrow worked on all phases of Shop Fabricating from 1923 to 1939, first with the Austin Co., and then with the Patterson & Leitch Gas Machinery Co., both of Cleveland. From 1939

Ross P. Yarrow

to the present time, he has been Superintendent of three Divisions of the Consolidated Iron-Steel Manufacturing Co.; namely, Republic Structural Iron Works, Republic Welding & Flame Cutting Co. and Duplex and Ideal Hanger Co. He has been an active member of the Cleveland Section since 1939 and was Chairman of that Section for two terms, 1944 and 1945. He was a member of the Executive Board of the Section for 8 years.

G. O. Hoglund

G. O. Hoglund is a native of Milwaukee, Wis. He graduated from the University of Michigan in 1925 with a degree of Bachelor of Science. For the next two years, Mr. Hoglund worked in the Bureau

G. O. Hoglund

of Aeronautics, Navy Department, Washington, D. C., designing airplane propellers. His next position was at the Naval Air Station in Lakehurst, N. J., after which he went to the University of Minnesota where he served as Assistant Professor of Aeronautics from 1927 to 1928.

Mr. Hoglund became affiliated with the Aluminum Co. in 1928 and has been with them ever since. Some of the departments with which he was affiliated are Central Metallurgical Division, Job Shop in Charge of Welding Procedures, and is now Head of the Jobbing Section of Alcoa Process Development Laboratories.

Mr. Hoglund has made outstanding contributions to the progress of the aluminum industry including developments of the techniques of resistance and arc welding of aluminum. He also holds several brazing patents in this field in the United States, Canada, Great Britain and South Africa. Mr. Hoglund is author of a number of technical articles which have appeared in the *SAE Journal*, *Mechanical Engineering* and THE WELDING JOURNAL. Mr. Hoglund has been active in the affairs of the AMERICAN WELDING SOCIETY, including the Chairmanship of the Pittsburgh Section. He served for two years as District Vice-President of the SOCIETY and was Chairman of the AWS Program Committee for a period of four years. He is now Chairman of the Sub-Committee of Light Alloy Filler Metal and a member of the ASME Welding Subcommittee of the Boiler Code Committee.

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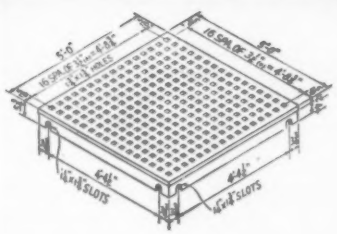
(EDITORIAL NOTE: Biography of District Vice-Presidential Nominees will be included in a later issue.)

BENDING BLOCKS
or
WELDING PLATENS
(SHIPYARD TYPE)



These blocks or platens are 5 ft. x 5 ft. as shown and are made of cast semi-steel. They are very useful for many purposes such as for bending tables, layout, welding and assembly-work, etc.

ACORN
IRON & SUPPLY CO.
Delaware Avenue at Poplar Street
Philadelphia 23, Pennsylvania

AWS-ASTM Specification

The "Tentative Specifications for Copper and Copper-Alloy Welding Electrodes," ASTM Designation B225–53T, AWS Designation A5.6–53T, has recently been revised and released in printed form for general use.

This specification covers completely all chemical requirements, deposit mechanical properties, methods of testing, preheat and interpass temperatures, standard sizes, etc., for copper and copper-alloy electrodes for metal-arc and the inert-gas consumable electrodes processes. The appendix describes very thoroughly usability of the various electrodes.

Single copies of this specification as well as the "Tentative Specifications for Copper and Copper-Alloy Welding Rods," ASTM Designation B259–52T, AWS Designation A5.7–52T, can be obtained free of charge from Ampco Metal, Inc., 1745 S. 38th St., Milwaukee, Wis.

NEWS OF THE INDUSTRY

Solar Promotions

Recently promoted to welding engineers at Solar Aircraft Co., Des Moines, are Fred W. Martinson and Gerald E. Hart. Both graduates of Iowa State College, they started with Solar as process engineers.

RWMA to Aid Worth-while University Researchers; Cancels Prize Contest

The Resistance Welder Manufacturers' Assn., Philadelphia, Pa., has announced the appropriation of funds for grants-in-aid to worth-while university researchers in order to secure basic information of value to the Resistance Welding Industry and to encourage the training of young men in Resistance Welding research.

The first grant will be for research on the subject of "Spot-Weld Aluminum with Single-Phase, 60-Cycle Current."

In view of this new Research Program of the Association, the annual Prize Paper contest for papers on resistance welding will not be conducted in 1954.

The Association has recently issued a number of new bulletins on various phases of resistance welding and several additional ones are now in the process of being completed. Announcement of these will be made very shortly.

Ultrasonics Applied to Aluminum

Ultrasonic soldering of aluminum and ultrasonic treatment of molten aluminum were demonstrated by Aeroprojects, Inc. Representatives of the United States Air Force and of the aluminum industry were in West Chester, Pa., on Friday, Feb. 19th, to witness the demonstration.

Aeroprojects showed how ultrasonics can assist fabricators to solder aluminum by production methods without the use of any flux. Soldered samples included are die castings, sand castings, sheet, wire and extruded aluminum.

A great increase in corrosion resistance of soldered joints has been produced as a result of extensive research and development work done by Aeroprojects' engineers. Means of obtaining minimum corrosion were explained.

An important demonstration was made of the application of ultrasonics in the treatment of molten aluminum. Ultrasonics remove gas pockets from the molten aluminum and refine the grain structure of aluminum ingots. Many difficult engineering problems had to be overcome to produce a successful treatment, said J. Byron Jones, President of Aeroprojects, as he discussed the background of this project.

Tremendous interest was shown in these new tools for the aluminum industry. The roster of persons and groups represented at the meeting was quite extensive.

A. O. Smith Appointments

John W. Spoor has been appointed to the newly created post of Assistant Division Manager of the Welding Products Division of the A. O. Smith Corp. He is succeeded as General Sales Manager by Richard W. Raney.

John W. Spoor

Mr. Spoor went with A. O. Smith in 1948 from the Brunswick-Balke-Collender Co. at Chicago, where he was assistant to the vice-president in charge of sales. He headed A. O. Smith's Product Service Division at Chicago before taking over Welding Products sales responsibility.

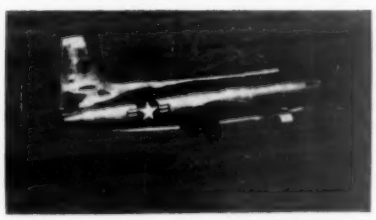
Formerly vice-president in charge of sales for Arms-Franklin Corp. at Youngstown, Ohio, Mr. Raney joined A. O. Smith in 1952 and was named Assistant Division General Sales Manager in 1953.

Erv A. Steidl, who was sales representative for A. O. Smith industrial safety grating in the Middle West during the past three years, now is the Welding Products Division's sales engineer for Wisconsin.

R. W. Ramey

Section of World's Fastest Plane Built by Alcoa

Two complicated sections of the world's fastest plane were fabricated at Aluminum Company of America's Jobbing Division at New Kensington, Pa. The sections consist of large tanks which are part of the rocket-powered Bell X-1A's fuselage. One of the tanks contains alcohol and the other contains liquid oxygen. Welding and heat-treating problems were worked out by Bell Aircraft and Alcoa engineers.



The Air Force X-1A recently set a new world speed record of more than 1600 mph. The X-1A, of conventional straight wing configuration, is 35 ft 7 in. long, 10

ft 8 in. high and has a wing span of 28 ft. Fully fueled, it weighs 8 tons.

Not intended to be a tactical airplane, the X-1A is a flying laboratory to investigate the aerodynamic effects of supersonic flight.

An "Equity" to Another Corner of the World

Another "Equity" Standard is on its way to service in another corner of the world. This time it is an "Equity" standard *Twin Screw* 45-ft tug, the *Freeport* by name, the place, the Port of Monrovia, Liberia. "Equity" standards are serving in Venezuela; Saudi Arabi; Saigon, China; the Belgian Congo, Africa and elsewhere.

This all-welded steel tug is powered by two General Motors series 4-71 Diesel engines with a $4^1/_2$:1 reduction gear. She carries the highest classification of the American Bureau of Shipping for River and Harbor Service.

The *Freeport*, whose dimensions are 45 ft length, 12 ft 5 in. molded breadth and 6 ft 10 in. depth, was fabricated at Equitable's Shipyard.

Equitable Equipment Co., Inc., operates from its main office at 410 Camp St., New Orleans, with its yard located in Madisonville, La.

Architectural League Medal

The Architectural League's 1954 Gold Medal in Engineering for outstanding creative design work in engineering is awarded by Daniel Schwartzman, President of the League, to Dr. Thomas C. Kavanagh (right), of the firm of Praeger-Kavanagh, Engineers, New York (also Professor and Chairman, Department of Civil Engineering, New York University). The award is shared with Camilo Piccone, Mexican engineer, and cites the design and erection of the Rio Blanco River Bridge near Vera Cruz, this structure comprising a basket-handle arch unique in the field of bridge architecture.*

The League's Silver Medal was awarded to the firm of Hardesty & Hanover of New York for their design of the S. E. Fourth Avenue Bridge in Miami, Fla. Honorable mention citations were received by Arsham Amirikian, Member AWS, of Chevy Chase, Md., for the design of a precast concrete warehouse, and by the firm of Parsons, Brinckerhoff, Hall & Macdonald, of New York, for their design of the George P. Coleman Memorial Bridge over the York River, Va. The Gold Medal in architecture was awarded the firm of Skidmore, Owings & Merrill of New York for their design of Lever House in New York.

* Paper published in the December 1953 issue of THE WELDING JOURNAL.

One Million Books on Welding!

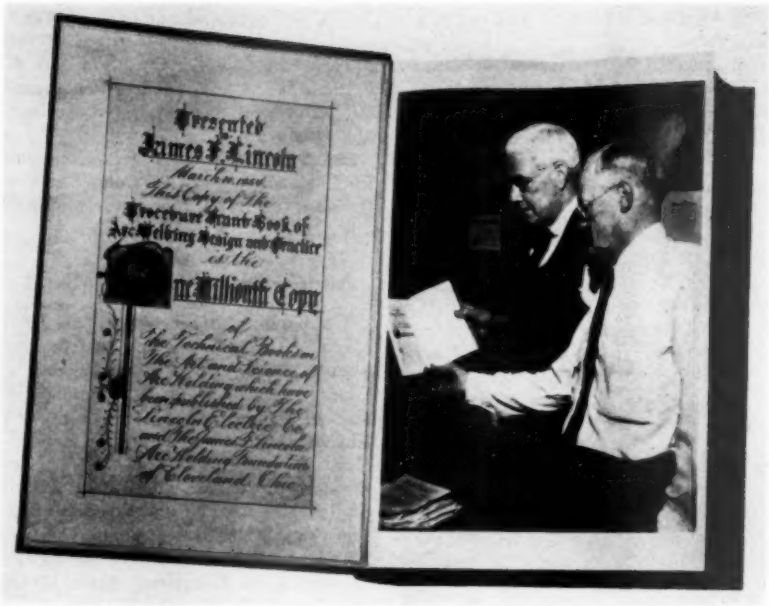
March 1954, marked the one millionth copy of the technical books on arc welding that have been published and sold by The Lincoln Electric Co. and The James F. Lincoln Arc Welding Foundation of Cleve-

land, Ohio. The one millionth book was a copy of *The Procedure Handbook of Arc Welding Design and Practice*. The copy marking this milestone, inscribed and illuminated, was presented to J. F. Lincoln, President of the Company, by A. F. Davis, Vice-President and Secretary, who has been responsible for this publishing activity.

The publication and distribution of technical books on welding have been the core of Lincoln's educational activity to stimulate the development and application of the arc welding process that dates back to 1917. Mr. Lincoln, in the early days of welding, recognized that it was going to be necessary to educate industry on the advantages of the welding process and how to use it.

The books have covered all phases of welding: instruction, process science, application in industry and agriculture for fabricating and maintenance, mechanical and structural design. The *Procedure Handbook* is now in its ninth edition. *New Lessons in Arc Welding*, an instruction book, is in its third edition. Twenty-four different titles have been published.

The books have been published and sold on a nonprofit basis as a service to industry. The Company's book policy, paralleling its manufacturing policy, has been to make the best possible book available at the lowest possible price. For example, the 1330 page Handbook, sells for only $2.00.



J. F. Lincoln, President of The Lincoln Electric Co., receives an illuminated and inscribed copy of "The Procedure Handbook of Arc Welding Design and Practice" from A. F. Davis, Vice-President and Secretary

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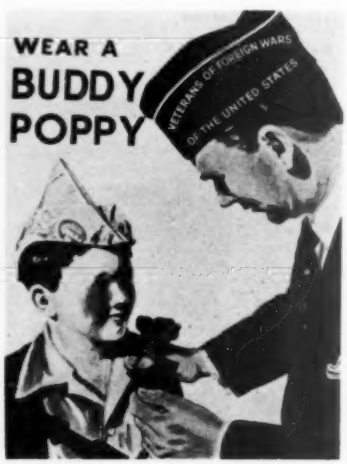
A DIVISION OF AIR REDUCTION COMPANY, INCORPORATED

"Bren/Weld" Sales Division

Kasson Die & Motor Corp., 32-14 Northern Blvd., Long Island City 1, N. Y., announces it is now the sales division for Brennen, Bucci & Weber Co., New York, manufacturers of "Bren/Weld" industrial welding equipment.

The company will be ready shortly to launch a full line of butt, arc and spot welders, incorporating a number of new ideas and engineering principles not heretofore seen in equipment of this type.



Left to right: W. R. Stevens, Sales Manager, Welding Division, Henry Harnischfeger, Executive Vice-President, M. O. Monsler, General Manager, Welding Division and B. Germischausen, in charge of display for the Harnischfeger Corp., and the driver, Lyle Prodell

Traveling Exhibit

Through means of a traveling exhibit which will go to 18 major cities, the Harnischfeger Corp., Milwaukee 46, Wis., will display and demonstrate what they claim is a revolutionary new line of P&H welding machines that incorporate many "firsts" in the welding field. They will also show several new types of electrodes, as well as a working model of a P&H welding positioner. The day preceding the exhibit will be devoted to a sales school for P&H distributors and their selling staffs.

The units to be exhibited include an entirely new a-c, d-c machine designated as the DA-200. The company claims that this is the first welder of its type ever offered, since it provides either ac or dc simply by throwing a switch. It is also designed to be used with single-phase power source.

Two new gasoline-driven arc welders, the WN-150 and WN-250, will also be displayed. The 150 is a compact unit powered by a powerful two-cylinder Wisconsin engine. The 250 is also entirely new and has, according to the company, a highly advanced arrangement of motor, generator, power take-off and idling device.

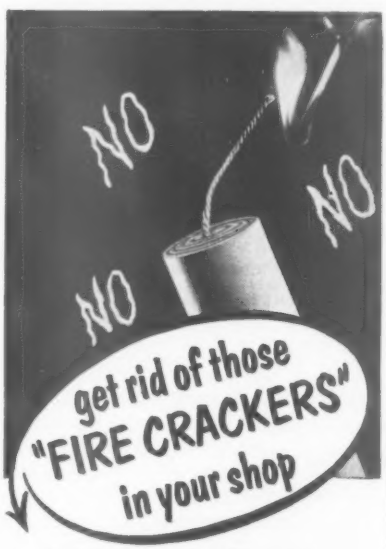
Another new unit in the P&H welding line that will be displayed is the DC-400—a 400-amp d-c rectifier welder. Features of this welder are said to include economy, silent operation, balanced-line loading and instantaneous remote control.

P&H also claims a "first" with their new TI-295 welder which will be on display for the first time. This unit has a top amperage of 300 and an open-circuit voltage of 75. Designed as an inexpensive, low-input intermittent welder, the unit is said to have all the advantages of a heavy-duty machine. It permits welding with low-hydrogen and stainless-steel electrodes.

This exhibition will be the first public showing of the CP-500-DC—a new, constant-potential d-c power source. The unit is designed to replace present rotating constant potential d-c equipment. Another unit in the making is a power source for submerged arc welding and for inert-gas, continuous-wire-feed welding guns.

The exhibition will also include actual welding demonstrations with three new high-speed "iron-powder" coating electrodes. These include the DH-4, a cutting and gouging rod; the DH-5, a single-pass heavy fillet rod; and the DH-6, a medium penetrating electrode that operates equally well in all positions.

Further information about the exhibition and the new machines may be obtained from The Harnischfeger Corp., Milwaukee 46, Wis.



What fire crackers? We mean those welding and cutting torches that keep popping—back-firing—burning out valve seats. Did you know that the ingenious new Smith "FLO-TROL" prevents reverse flow of acetylene—protects torch against backfire and burned out seats?

Write us a card . . . NOW!

SMITH WELDING EQUIPMENT CORPORATION

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Users of
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Welding
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Big Book

HERE, at last, is a catalog designed for a busy man to use—good, clear illustrations with contours and dimensions plainly shown—well-organized listings with prices right alongside the specifications.

Nearly 700 resistance welding electrodes are listed for almost any welding job. The catalog also lists water-cooled holders of every style and size, seam welding wheels, alloy rod and bar stock.

If you do resistance welding, you need this book. Write for your free copy today.

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Division of Fansteel Metallurgical Corporation
11644 Cloverdale Ave. • Detroit 4, Michigan

NEW PRODUCTS

Seamless Tubing for High Temperature Applications

An alloy steel, designed for use in equipment operating under conditions involving high stresses and temperatures, is now commercially available in seamless tubular form, according to an announcement made recently by the Tubular Products Division of the Babcock & Wilcox Co., Beaver Falls, Pa.

Impact Tests of Welded Austenitic Stainless Steels

Impact data from tests at room temperature, −105 and −320° F of various types of stainless steel plate material and weld metal permit comparison of the effect of weld rods of different analyses and the effect of heat treatments. The weld metals tested included AISI Types 301, 302, 304, 308, 310, 316, 317, 318 and 347 (the last with both lime and titania coated electrodes). International Nickel Co., Inc., New York 5, N. Y., Bulletin A-165, 2 pages.

Eutectic Develops New Bronze Electrode

A highly versatile "Bronzochrom" electrode, EutecTrode 1850, has been developed for high strength joining and for frictional and corrosion resistance on a wide variety of base metals by the Eutectic Welding Alloys Corp.

The electrode produces copper base deposits alloyed with silicon, manganese and chromium, etc., which increase tensile strength and give additional resistance to wear and corrosion. Hardness as deposited is 130–140 Bhn and this can be increased by cold working to 230–240 Bhn.

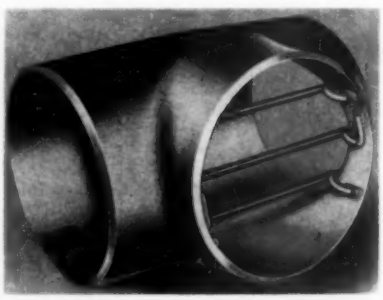
The special formulated flux coating with its strictly controlled rate of disintegration restores to the deposit elements lost across the arc. The weld metal, as deposited, thus contains the elements and manifests the desirable properties required to give the necessary strength and resistance to operating conditions.

The electrode is recommended for joining and overlaying steel, cast iron and copper alloys. It is excellent for the various combinations of ferrous and nonferrous metals including stainless steel, nickel and nickel alloys. It is claimed to be harder and five times as rapid to apply as conventional bronze brazing rods. It is noncracking, fully machinable, minimizes warpage and distortion of the parent metal, gives twice the wear resistance of phosphor bronze and has a tensile strength up to 90,000 psi according to the base metal upon which it is used. The speed of application is phenomenal and no special skill is required to produce sound homogeneous welds.

For further information write Eutectic Welding Alloys Corp., 40-40 172nd St., Flushing 58, N. Y., for free explanatory leaflet TIS 2190.

Wandering "Pigs" Fenced In

The nation's vast network of natural gas transmission lines are regularly cleaned out for if foreign matter were allowed to accumulate in them, flow rates would be reduced and possibly operating troubles might develop. Mechanical "pigs" of various types are employed for cleaning purposes. They are inserted at launching stations and recovered in traps spaced at intervals of 20–30 miles. The pigs travel at speeds up to 30 mph, moved along by



Alignment bars in special Tube-Turn tees keep "pigs" from making detours

pressures of 5–6 psi. Difficulties frequently arise when the pigs reach side outlets or branches, which they have an inclination to enter. Pigs of the compressed wire-brush type may be seriously damaged at these points. To aid pipeliners in solving the irksome problem of wandering pigs, Tube Turns, Louisville, Ky., has made available a Tube-Turn tee equipped with alignment bars. The bars are made from heavy wall pipe and shaped to provide flexibility. They follow the elastic movement of the tee as pressure is applied, and there is no tendency to break attachment welds.

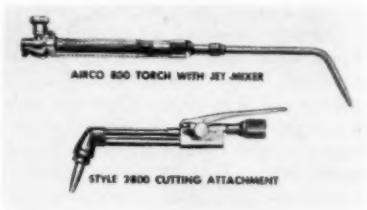
The tee equipped with bars is a "special," but can be supplied for pipeline applications on reasonable notice.

Tube Turns also furnishes the elbows, reducers and other fittings required in the construction of pig launching stations and traps.

Tube Turns is a division of the National Cylinder Gas Co., Chicago.

Cutting Attachments and Gas Mixer

Air Reduction announced the availability of new cutting attachments and a new jet-type mixer which greatly extend the cutting, welding and heating range of the Airco Style 800 general purpose oxyacetylene torch.



The new jet mixer handles all welding and heating tip sizes with complete flame stability. Sputtering, popping and flashback are minimized as a result of the modern design features built into this mixer. Rubber sealing rings provide a gastight seal with hand tightening; no wrench is needed, thereby saving valuable time for tip changes.

The new cutting attachments, known as Airco Styles 1800 (medium duty) and 2800 (heavy duty) provide added versatility and cutting range to the Airco 800 torch. The Style 1800 cuts steel up to 6 in. thick and the Style 2800 cuts steel up to 8 in. thick. Standard Airco cutting tips, any one of the tips designed for the Styles 3000 and 9000 hand-cutting torches, may be used with the attachments. Consequently, the Style 800 torch, equipped with the heavy-duty cutting attachment, can now handle a wide variety of cutting and gouging operations not normally possible with a cutting attachment. The new attachments use a diaphragm-type valve for the control of cutting oxygen. This valve completely prevents gas leakage and eliminates the need for valve packing.

Both attachments are finely balanced tools for operator ease and are ruggedly constructed of heavy forgings in both the heads and bodies. The rubber gas sealing rings provide a firm gastight seal. The mixer in the cutting attachment can be removed easily for cleaning or replacement, if necessary.

For further information, request a copy of ADC 837 by writing to Air Reduction Magnolia Co., PO Box 319, Houston, Tex.

the complete line FOR ALL YOUR NEEDS

YOU CAN specify TUBE-TURN Welding Fittings and Flanges and know that you will get the *exact* answer to your specific piping problem. This leading line includes more than 4,000 items, in all piping materials . . . carbon steels, non-ferrous metals and alloys . . . schedules and sizes to match *your* design.

Carbon steel elbows, for example, are available from ½" to 42" diameter. Your nearby Tube Turns' Distributor provides this *complete* line promptly and backs it up with the thorough Engineering Service and how-to-do-it information of Tube Turns.

The Leading Manufacturer of Welding Fittings and Flanges

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A Division of National Cylinder Gas Company

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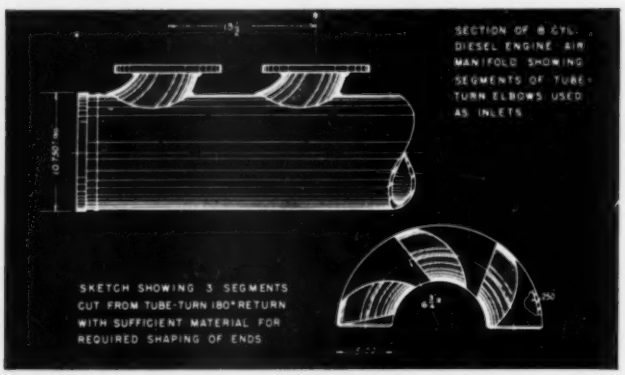
Here's *Service* to help you solve your piping problems

TUBE TURNS' Engineering Service Division makes available a vast fund of experience for the solving of design and operating problems in piping. For example:

An engine builder was fabricating manifolds from formed plate. Poor fitup was a problem and costs were excessive. Tube Turns' engineers suggested using TUBE-TURN Welding Returns, cut with templates as shown in the sketch. Costs came down. The dimensional uniformity of TUBE-TURN Welding Returns and Elbows simplifies fabrication jobs such as this because they can be cut to odd angles and adapted to special requirements.

TUBE TURNS IS AT YOUR SERVICE!


SECTION OF 8 CYL. DIESEL ENGINE AIR MANIFOLD SHOWING SEGMENTS OF TUBE-TURN ELBOWS USED AS INLETS
SKETCH SHOWING 3 SEGMENTS CUT FROM TUBE-TURN 180° RETURN WITH SUFFICIENT MATERIAL FOR REQUIRED SHAPING OF ENDS

Header connection design problem solved by Tube Turns' Engineering Service.

TUBE TURNS, Dept. O-3
224 East Broadway, Louisville 1, Kentucky

Please send me free copy of Pipe and Fittings Materials.

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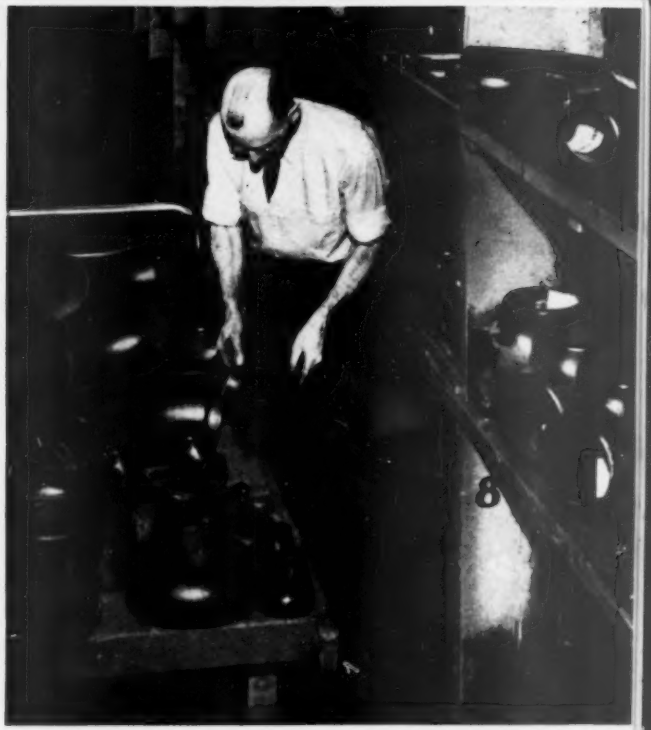
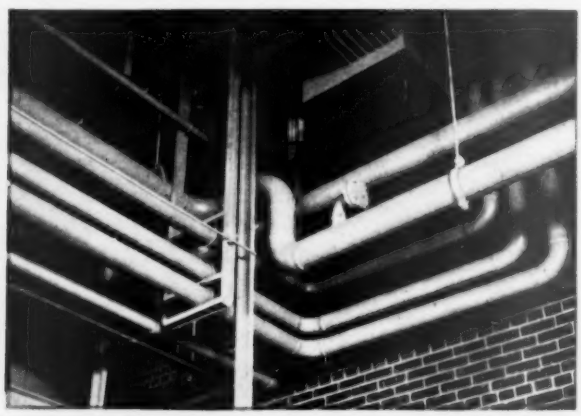


YOUR NEEDS in welding fittings and flanges are met *promptly* by your Tube Turns' Distributor. This one reliable source can fill *all* your requirements from the world's most complete line of welding fittings and flanges comprising more than 4,000 items.



MANY USES. Piping in this modern edible oil plant conveys natural gas, hydrogen, oil, steam, cooling water, refrigerants, air and vegetable oil. All critical lines are welded and TUBE-TURN Welding Fittings are used for directional changes. Pipe sizes vary from 2" to 16".

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Philadelphia	Seattle
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Los Angeles	Dallas
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"tt" and "TUBE-TURN" Reg. U. S. Pat. Off.

TUBE TURNS

LOUISVILLE 1, KENTUCKY

A DIVISION OF NATIONAL CYLINDER GAS COMPANY

Bench Model Welding Turntable

The latest in precision welding positioners is this Aircrafter Bench Model Turntable which can be adjusted to hold work in any position desired, from horizontal to vertical. The Aircrafter Turntable provides smooth stepless speed change from 0 to 7 rpm or a greater speed ratio, if desired, is obtained by a gear change at slight additional cost.

Although primarily designed for Heliarc and precision aircraft type welding it will, if work is properly balanced, carry a 100-lb load. Many aircraft companies are now using the Aircrafter Welding Turntable and find it reduces rejects and increases output up to 25% while enabling the worker to turn out much better welds. Can be used for Heliarc, arc or gas welding as well as brazing and soldering.

The complete Aircrafter Welding Turntable weighs only 20 lb and can be easily placed anywhere in the shop. The turntable, itself, is 7 in. in diameter and speed is set by a rheostat with a foot-control switch for "on and off" or ratio control.

The Aircrafter Welding Positioner and Turntable is manufactured and guaranteed by the Superior Welding Co., 3410 E. 14th St., Los Angeles 23, Calif.

Aircrafter Bench Model Welding positioner and Turntable shown in horizontal position with foot control. Rheostat gives variable speed control from 0 to 7 rpm

KSM Battery Unit

A new portable Battery Unit supplying independent power for welding studs up to 1/2-in. base diameter is now offered by KSM Products, Inc., Merchantville, N. J. This unit is recommended for stud welding where no power lines are available or when existing lines are unable to handle stud-welding current in addition to their regular load. Completely integrated and independent of power lines (except for recharging), the unit is readily adaptable to all stud-welding installations within its power capacity.

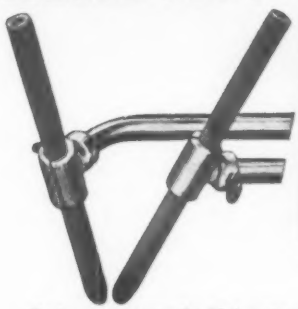
A tap selector switch, calibrated in weld-base diameters, regulates current supply. An "off" position eliminates external terminal short-circuiting when the unit is not in use. Output terminals for connections to control box consist of two threaded studs and wing nuts; an ammeter indicates charging current, and a circuit-breaker switch protects equipment from excessive input charging current.

Twelve heavy-duty wet batteries of 150-amp-hour rating are kept charged, when connected to a 115-v, 60-cycle, single-phase a-c source (15-amp line minimum), by an integral automatic charger which test-checks open-circuit battery voltage every 10 min and charges when terminal voltage is below normal. Access to 12 batteries, confined by steel partitions to prevent accidental damage to other sections of the unit from battery acid, is made quick and easy by piano-hinged covers on both sides of the unit.

Complete information and specifications available from the manufacturer, KSM Products, Inc., 322 Woodland Ave., Merchantville 8, N. J.

BECKER

KEEN-ARC CARBONS



FOR CONSISTENTLY GOOD RESULTS

Designed to carry the high currents necessary for intense heat, BBB Keen-Arc Carbons produce a fine-grained weld of high tensile strength. They give a smooth, steady "flowing" flame which does not wander and which is concentrated at the desired focal point. Flame temperature is easily and accurately adjusted by merely changing the ampere input, and heavy copper coating permits gripping at extreme ends—eliminates frequent and periodic resetting.

A COMPLETE LINE OF CARBON WELDING SUPPLIES including carbon and graphite electrodes, carbon rods and plates, welding paste, etc. Write for catalog.

BECKER BROTHERS CARBON CO.

3450 South 52nd Ave. Cicero 50, Illinois

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Welding Department
P. O. Box 868, Pittsburgh 30, Pa.

Name_____
Company_____
Address_____
City_____ State_____

WJ-4-54

The Inquiring Weldor

BY JOHN WILSON

Question: Has hard-surface welding advanced as much with "low-heat" welding rods as production and maintenance work on steels, aluminum, etc.?

Gerald W. Sharp, Linden, N. J. Let me tell you about "Operation Piercing Punch" and I think you'll agree it has. We had tried hard chrome high-heat plating on a shell punch costing $80. The life of the punch was 2,000 shells. Then, using EutecTrode 680 and 6 HSS we increased the life of the main punch to 12,000 shells. The 680 layer acted as a "shock-absorber" for the subsequent layer of EutecTrode 6 HSS. The 680 also withstood the high heat of the billet (2350). No subsequent heat treating was necessary, we got a Rc 50 reading and with heat treating could increase this to Rc 60-62. Finally, using ChamferTrode we made a tremendous saving in time and money by cutting down machinist's grinding time. That's really hard-surface welding work at its profitable best.

Question: Is welding equal to salvaging seriously broken fully machined dropped forge steel hardened to BR588?

George Gill, Phila., Pa. *Non-fusion* welding is! In our foundry, we welded two crane wheels, got that high a Brinell reading without distortion and with high tensile strength. After test welds showed only "low Amp" EutecTrode 660 would do the job, we "veed" deeply with CutTrode which gave us automatic "pre-heat" and permitted continuous welding. We let the wheels cool uncovered, took base and weld metal Brinell readings, and found "660" gave us just about the BR340 hardness of the steel. We had no distortion of flanges or bores on the wheel facings. It was almost impossible to find the welds after grinding. After a year's 18-hour-a-day service, the wheels show none of the bad wear effects previously noticed on the chilled cast iron wheels.

Question: How can you simplify delicate silver brazing on stainless steel and prevent the oxidation problems and cracking which occur so often?

William Slage, Canfield, Ohio. We weld thousands of drink dispensers joining stainless and copper components. Using "Non-Fusion" welding and EutecRod 1800 instead of silver brazing we are able to join 2 3/4 inch solid stainless steel faucet bodies to several 3/16 inch OD, .020 wall thickness, 304 stainless steel tubings. Since "1800" bonds at a lower temperature than silver alloys and "wets" better we do not have "burn throughs," we use less alloy, cut our costs and have not had a reject in over five years. Also, the "1800" welds are so neat that expensive machining and finishing costs have been virtually eliminated. Even though our equipment is subjected to notorious corrosive and electrolytic action, the unique cadmium-free EutecRod 1800 always wins the approval of government inspections.

EUTECTIC
EUTECTIC LOW TEMPERATURE WELDING ALLOYS clear up welding headaches . . . minimize warping, distortion, stresses and embrittlement. For time and money saving help on welding steels, aluminum, cast iron . . and all "problem" alloys at temperatures below base metal melting points . . . and for stronger, safer production and maintenance welding just write "Welding Data Bank" for your free copy of "1954 Welding Data File" on your jobs.
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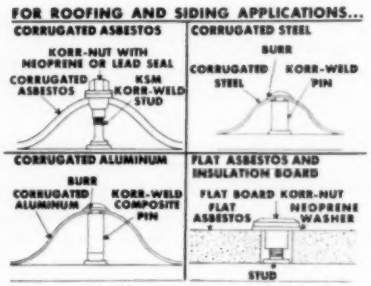
EUTECTIC WELDING ALLOYS CORP.
40-40 172nd St., Flushing, N. Y.

Stud Welding Methods

Significant improvements, included in the design of KSM welding studs for the construction industry, provide strong, corrosion-resistant fasteners for corrugated asbestos, steel and aluminum, flat asbestos and insulation. Also, these new KSM fastening methods are particularly suited to all types of curtain wall construction. All stud welding applications are from the top side of roof or outside of wall. No interior scaffolding is required, nor is it necessary to reach around the material to secure side-lap fasteners. Interior finishes are clean. Studs are not visible from inside.



Contractors and applicators can obtain complete information on KSM Korr-Stud Welding Methods for erection of corrugated asbestos, steel and aluminum sheet siding, electrical construction, and sprinkler and plumbing installation by writing to KSM Products, Inc., 321 Woodland Ave., Merchantville, N. J. Requests should specify the type of corrugated roofing or siding material being considered.

A-C Arc Welder

A new low-cost "utility" a-c arc welder, designed to meet the needs of shops that do not require "production-type" capacity, is being introduced by Marquette Manufacturing Co., Inc., Minneapolis.

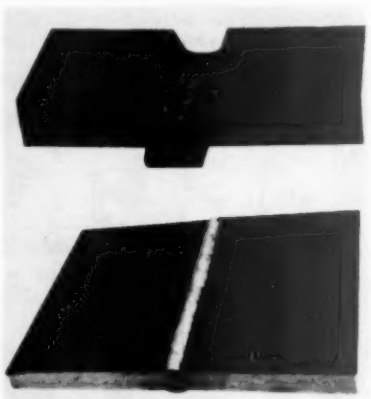
This welder, the Redi-Arc, readily handles up to 5/32-in. electrodes, and can be used wherever 220 v, 60-cycle, single-phase current is available. The Redi-Arc is claimed to have the highest usable capacity possible in a unit of its size and rating, yet its input demand is well within the limits of a regular 3-kva line transformer.

Featuring the "instant arc" for easy arc striking, the Redi-Arc produces a stable, surge-free welding arc easily handled even by nonprofessional or occasional welders. It is built to equal or exceed NEMA and REA specifications, and can be used in sheetmetal shops, body shops, repair and maintenance departments, farm shops and home workshops.

For more information, write Marquette Manufacturing Co., Inc., 307 E. Hennepin Ave., Minneapolis 14, Minn.

Jetweld 2

The Lincoln Electric Co. has announced the second electrode in its newly developed line of electrodes with powdered metal in their coatings. Called Jetweld 2, the new electrode is designed especially for welding butt and deep groove joints. It is a companion electrode to Jetweld 1, designed for welding horizontal and flat fillet joints.



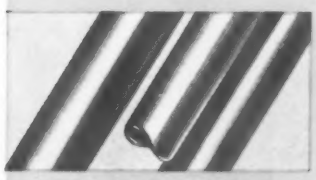
These new electrodes with powdered metal in their coatings are claimed to be a radical departure from electrode designs that have been the standard for the past 20 years; a departure as important as the step from bare electrodes to coated electrodes.

Jetweld 2 is designed for high-speed welding of butt and deep groove joints to weld at the lowest possible cost per foot of weld with excellent X-ray qualities, easy slag removal, top physical properties, smooth appearance and low crack sensitivity. These objectives, Lincoln engineers say, required, as with Jetweld 1, a basic change in electrode design, the powdered metal coating.

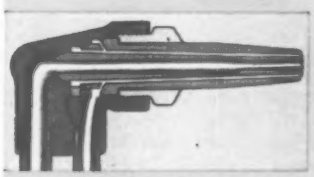
Jetweld 2, it is claimed, will weld butt and deep groove joints up to 50% faster than conventional electrodes. It operates

NOW...to give you MORE in the TORCHWELD® "75"

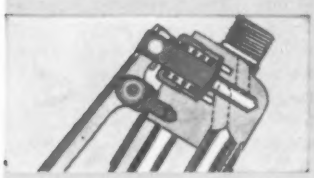
TORCHWELD "75" features that add up to better performance



Triangular arrangement of tubes for exceptional rigidity and strength. Silver brazed joints never jar loose.

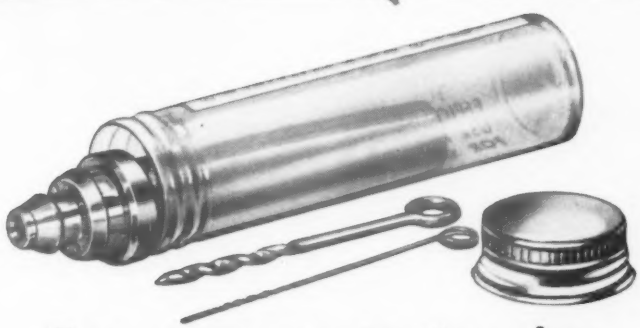


Stainless steel head. Withstands severest cutting conditions. Won't distort under extreme heat.



Leakproof Diaphragm Valve is the simplest, safest, most economical type of high pressure oxygen control ever designed.

(Not shown.) Sure-grip grooved and fluted handle. Natural action high pressure control lever that gives utmost sensitivity and accuracy of control.



Two-way protection that multiplies tip life 3-5 times

The construction *FACTS* behind Torchweld "75" design bear out its record of longer life, better performance, reduced costs.

But now a new protective measure will increase the already great value you get from the Torchweld "75".

We are now packaging each "75" Cutting Tip in a protective plastic container. Not so much for safety in shipping—*but as an invitation to cutting operator to return each tip, when not in use, to this plastic container.*

This simple, easy measure will keep the finely finished seating surfaces of the tip from contact with dirt and grit, will preserve it from careless handling . . . will multiply the useful efficient life of the tip *three to five times over.*

In addition, two NCG tip cleaners are packed with the tip, for the proper cleaning of the preheat and high pressure oxygen passages. Another measure that will greatly lengthen tip life.

We think enough of "75" Cutting Tips to spend a lot of care in their preparation. We think enough of your investment in these tips to want to protect it to the end. Compare the Torchweld "75" Cutting Torch —and tips—with anything you have used before. Contact an authorized NCG dealer, or NCG district office, or write the address below for a no-cost, no-obligation demonstration. Don't wait, do it today.

NCG®

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NATIONAL CYLINDER GAS COMPANY

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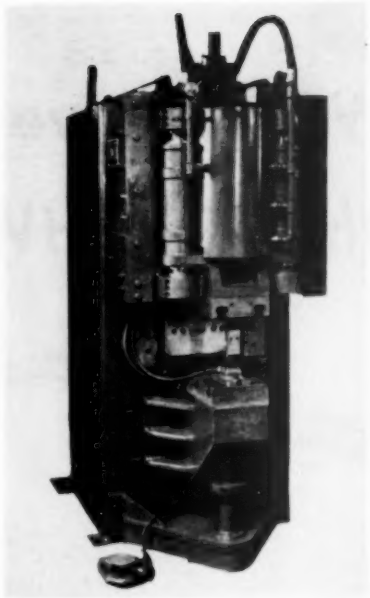
on either ac or dc, with operation on ac preferred. The bead has excellent wash-in. Slag removal is excellent. High ease of operation and low fatigue factor result from the drag technique used with the heavy coatings. Spatter is reduced to a minimum, and the smooth appearance on cover pass welds meets automatic welding standards.

Its physical properties recommend the electrode for X-ray work, and it can be used on steels with hardening tendency. Typical values are: yield, 55,000 psi; tensile, 66,000 psi; elongation as welded, 28%; stress relieved, over 30%. Charpy keyhole notch impact values average 37 ft-lb at room temperature, and the transition temperature is below −100° F. The electrode is classified at E6020 and meets all E60 classifications in the downhand position. It is available in 5/32, 3/16, 7/32 and 1/4 in. diameters.

New High-Pressure Projection Welder

A new projection welder which combines high welding pressure with fast ram action is announced by the Universal Welder Corp., Cleveland 4, Ohio.

Particularly suited to the welding of light metals and to other applications requiring high pressures and quick follow-up, the machines are rated at from 5000 to 18,000 psi over a size range of from 250 to 500 kva.



A novel ram design in the new unit combines low inertia with virtually frictionless motion. The ram—equipped with a double-acting pressure head of the zero-friction diaphragm type—is mounted in an aluminum frame. The latter is guided on two round-column vertical shafts by sleeves incorporating multiple linear ball bearings. These bearings eliminate extensive wear adjustments required by conventional cam-guided machines; a new bearing surface is provided by merely rotating the guide columns.

The units have welding strokes over a 1/2–2 in. range and may be had with an adjustable retraction-stroke ram providing 3–6 in. additional travel. The latter is actuated by an air cylinder through toggle linkage and is counterbalanced for a smooth, shockless stroke.

The new welders are available with standard throat depths of 12–48 in. in 6-in. steps. The adjustable lower knee may be a double-screw, knee-braced type or equipped with a hydraulic jack.

Plastic Squeeze Dispenser

Special Chemicals Corp. of Ossining, N. Y., announces Kwikflux No. 54 now available in a new Kwikfluxer Applicator Container made of pliable plastic with screw cap tip and is refillable.

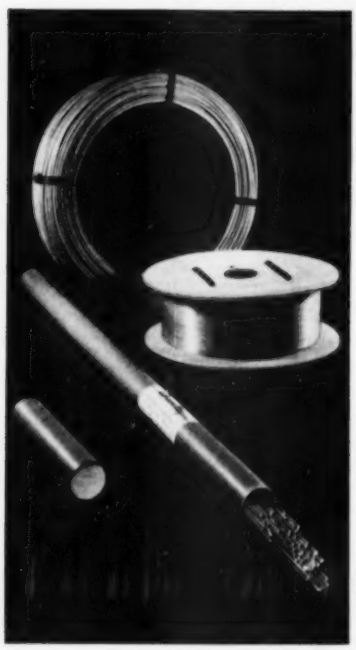
The manufacturer claims the Kwikfluxer is economical, quick and convenient to use. All that is necessary is to pierce the tip, squeeze and apply to work. No brushes, no jars, no breakage, no waste, no skin contact, no sores.

Small opening eliminates waste from rapid evaporation and hardening. Saves time and flux. The Kwikfluxer Applicator holds 12 full ounces of Kwikflux No. 54 for silver brazing and hard soldering. Ready for use, needs no diluting for most applications.

For full information, write to Special Chemicals Corp., 100 S. Water St., Ossining, N. Y.

Automatic Flux Valve

A new type Automatic Flux Valve has been announced by the C. B. Herrick Mfg. Corp., of Cleveland, Ohio. The new valve gives positive control of the flow of flux on automatic, submerged-arc welding applications.



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CORPORATION
QUALITY CONTROLLED
STAINLESS STEEL
WELDING WIRE

▶ available in Spools, Coils and Lengths

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Welding wire by Drawalloy is made to an exacting high standard . . . a high standard established by long-experienced welding men who know welding wire and how it must work. Set-ups for automatic and semi-automatic welding take more time than regular welding. That's why you want to be sure of satisfaction *before* you start. This *reliable* wire is weld-tested to give the best results every time. Write today for complete information and prices.

WELDING SUPPLY DISTRIBUTORS: Get prompt delivery on all sizes and grades of stainless and tool steel wire for gas and inert arc welding.

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WELDING FITTINGS
Stainless Steels · Monel · Nickel · Aluminum
4027 stock items
You can plan for economical, efficient butt-welded corrosion piping with assurance of quick availability of FLOWLINE Fittings. Delivery can be made from stock by leading distributors. Made in sizes ½" through 12" — Schedules 5S, 10S, 40S, 80S — Stainless Types 304, 304L, 316, 347; Monel, Nickel, Aluminum. Seamless fittings with all the plus values . . . at no extra cost.
WELDING FITTINGS CORP.
NEW CASTLE, PENNSYLVANIA
World's Largest Manufacturer of Stainless Welding Fittings
Remember
FLOWLINE
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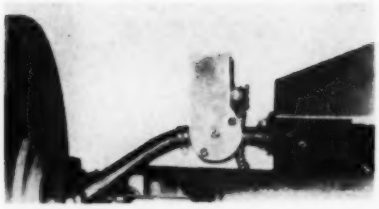
PIPING PROBLEMS



Its use eliminates the waste of flux and also prevents the possibility of the operator neglecting to start the flux flow. Using the Herrick Flux Valve, the operator knows when the electrode is in direct contact with the work since the flux will not flow until the electrode contacts the work.

The valve has been designed to operate with all Type LAF-2 Automatic Welding Heads. When the operator turns the "Inch Down" switch to weld position, the flux automatically begins to flow after the electrode contacts the work. When the operator reverses the "Inch Down" switch, the flux flow automatically stops.

A unique feature of the Herrick Flux Valve is its compact design enabling it to be attached directly under the Flux Hopper.

The valve is so designed that it will not jam either in an open or closed position.

Complete wiring diagrams are enclosed with each valve.

Flash Trimming Machine

The Morton Special Vertical Flash Trimming Machine is used in removing the flash from the inside and outside of thick-wall welded rings and cylinders. This trimmer has a capacity of 1/4 in. minimum to 3/4 in. maximum thickness and from 6-in. minimum inside to 24-in. maximum outside capacity. It is made in sizes for trimming 4, 7 and 10 in. lengths of work. This machine is entirely hydraulically operated. The piece to be trimmed is placed over the inner clamp member and ram. A foot-operated push button starts the cycle. The inner ram member moves forward hydraulically to clamp the cylinder. Both rams automatically cut on the draw-cut downward

stroke. At the end of the cutting stroke the inside ram and clamp dies move to unclamp position and the rams return automatically to load position.

An alternating-current 15-hp motor and control equipment provides suitable power for operating this machine. An approximate cutting speed of 60 fpm with a 2 to 1 quick return is used. Morton's special tool holders and standard flash trimmer cutting tools are used with this machine.

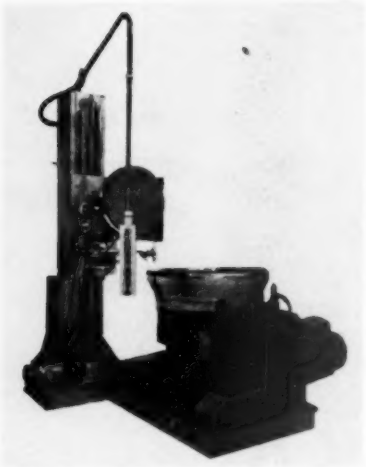
Morton Manufacturing Co., Muskegon Heights, Mich.

Girth Welder

A standard heavy-duty positioner is mounted on a common base with a vertical column for very flexible weld head positioning.

The positioner is designed and built to machine tool precision standards and provides a rugged and accurate unit with smooth wide range table rotary speeds. The standard bored hole goes completely through the table without interference, making possible the application of hydraulics, air, water or gas into special fixturing through rotary joints. Jig drilled and tapped holes on the table surface offer interchangeable fixture mounting applications.

The vertical column supports a power-operated saddle which is guided by V-



type rollers. On the front of the saddle is mounted single or multiple universal head mounting arms. The arms impart manual horizontal and radial movement to the welding equipment.

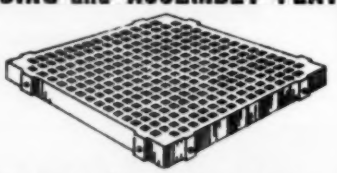
All machine operating controls and welding cycle can be operated from a swinging pendant station to give flexibility to the over-all operation. All machine movements are protected from overtravel by use of electrical interlocked limit switches. Unlimited welding problems can be solved with this new Morton Universal Girth Welder. Morton Manufacturing Co., Muskegon Heights, Mich.

Washers Reduce Silver Brazing Costs

Silver brazing costs are reduced as much as 60% when new L-M edgewound washers are used, according to their manufacturer Lucas-Milhaupt Engineering Co., Cudahy, Wis. Through important engineering advances, incorporating the No Tangle-Notch Coil feature, washers are produced without die charges or tooling costs and eliminate scrap waste common to blanking.

Flat wire alloy is wound on edge and then cut partially through. The coil of

WELDING and ASSEMBLY PLATENS



Floor plates — bending tables — layout tables — straightening tables — dog blocks — bending blocks — blacksmith blocks. 5'x5' — 6'x6' — 6'x8' — 6'x10' — 5'x10' — 6'x12' — 3'x3' — either 6" or 7" thick.

STAHL EQUIPMENT CO.
94 Washington St., Brookline Village 46, Mass.

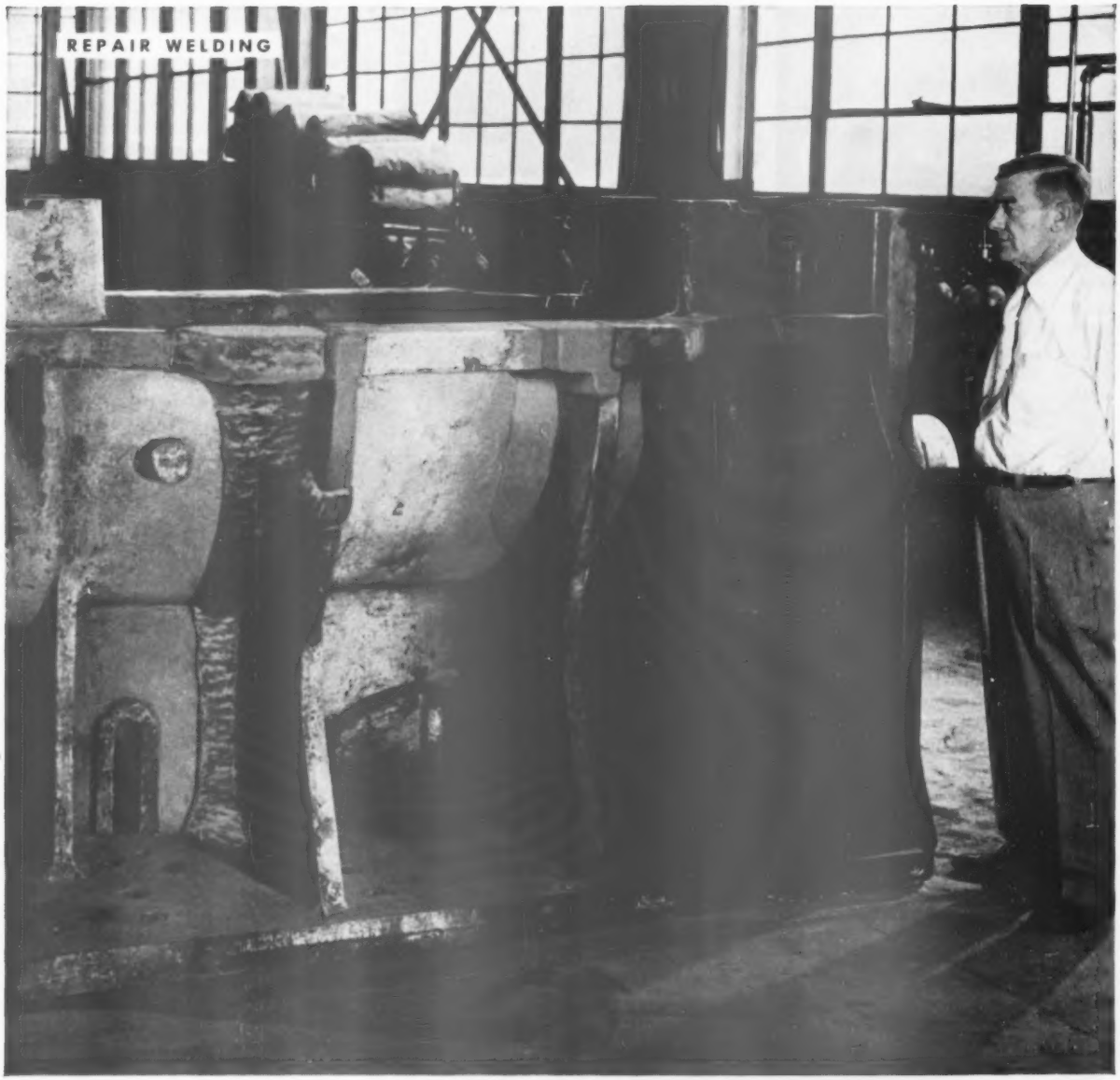
UNITED STATES TESTING COMPANY, INC.

INSPECTION and TESTING of Welding and Weldments

QUALIFICATION of Procedures and Operators

Main Laboratories **Hoboken, N. J.**

Boston - Chicago - New York - Philadelphia - Providence



"Don't scrap it . . . braze-weld it" paid off handsomely when this inner ram was twice repaired.

Braze welds twice save press part from scrap heap

This is the inner ram of a 500-ton press for shaping automobile fenders, quarter panels, floors, etc. It weighs 10 tons.

Twice it cracked, in different places. And twice braze-welding repaired it. It would have cost thousands of dollars to replace this part.

Punch Press Repair Corp., 1975 Hilton Road, Ferndale 20, Michigan, did both jobs. Each time they used Tobin Bronze*-481 Welding Rods. They say, "Welding with Tobin Bronze saves us time. It's economical and dependable, too. Braze welds need less preheating . . . high stresses don't build up. Tobin Bronze flows freely, 'tins' quickly, gives strong, sound bonds."

Tobin Bronze-481 Welding Rod is widely used for repair welding of cylinders and other locomotive parts . . . automobile engine blocks . . . cast-iron pipe . . . machinery parts and other equipment.

ANACONDA Welding Rods for *many* types of repair and production jobs are available from distributors throughout the United States. *The American Brass Company, Waterbury 20, Connecticut. In Canada: Anaconda American Brass Ltd., New Toronto, Ont.*

*Reg. U. S. Pat. Off. 54137

ANACONDA®
welding rods

AnacondA Copper-372 • Tobin Bronze-481 • AnacondA-997 (Low Fuming) Bronze • Nickel Silver-828 • Cupro Nickel-826 • Everdur-1010 • Ambraloy-928 • Phosphor Bronze-351 • Phosphor Bronze-354

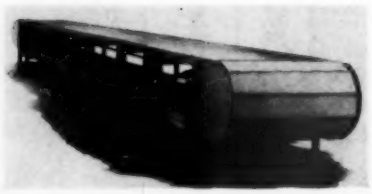
washers is then shipped intact. When used, each washer is snapped from the coil and preplaced on work as needed. Unused washers remain in coil form.

The new washers are wound in a variety of sizes and alloys. Diameters up to 4 in. ID are held to close tolerances in thicknesses from 0.010 upward. Available in Easy Flo and Silfos as well as copper, bronze and the more commonly used alloys.

Steel Conveyors

Steel belt conveyors in widths from 12 to 60 in. and any length from 10 ft up have been added to the Weld-Bilt line of materials handling equipment engineered and manufactured by West Bend Equipment Corp., West Bend, Wis.

Long life and low operating friction are assured with the Weld-Bilt conveyors which are made up of 14-gage cold-finished steel sections which move on ball-bearing



rollers and grease-sealed hardened runways. The sections are continuously hinged with $^1/_8$-in. bolt pins.

Changes in belt lengths are made easily by the simple addition or removal of belt links and bolted channel steel supporting structure units.

Further information is available from West Bend Equipment Corp., 349 Water St., West Bend, Wis.

Hard-Facing Electrodes

A new series of general service iron-base d-c hard-facing electrodes having a new metallic coating that provides improved weldability and arc stability is now available from Wall Colmonoy Corp., 19345 John R St., Detroit 3, Mich.

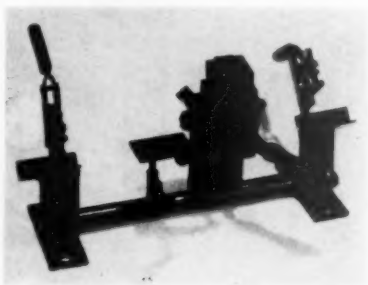
The electrodes which are made up of a chromium, boron and iron composition are available, in two types: Colmonoy No. 1 for hard-facing applications where extreme impact with high-abrasion resistance is required, and Colmonoy Special No. 1 for applications where extreme abrasion and impact resistance are required.

In addition to the improved weldability and arc stability features, the electrodes provide dense deposits that do not require cleaning or slag removal by chipping or brushing before welding on successive deposits. The deposits have uniform hardness and provide improved impact and abrasion resistance over former comparable iron-base alloy electrodes.

Another important feature of the new hard-facing electrodes is that they can be used in applications where the deposit must be welded on vertical surfaces.

Welding Accessory

Erico Products, Inc., 2070 E. 61st Place, Cleveland 3, Ohio, has announced the latest development in the Caddy Arc Welding Accessory Line, the L-145 Bench Fixture.



The L-145 is used for making rapid production Cadweld connections of the welding cable to all Caddy arc-welding accessories plus splices of the welding cable.

Descriptive sheet on the L-145 Bench Fixture available on request.

Tough Enough
. . . TO STAND UP UNDER SEVERE IMPACT AND ABRASION!
MANGANAL
U. S. Patents 1,876,738 - 1,947,167 - 2,021,945
11%-13½% MANGANESE-NICKEL STEEL
HOT ROLLED PLATES
Reduce Downtime on
TRUCK BOTTOMS
LAST MUCH LONGER THAN NEW BOTTOMS, TOO!
"HOW TO DO IT" DETAILS SENT ON REQUEST
Mill Depot Stocks: Newark • Wilkes Barre (Forty-Fort)
Indianapolis • Chicago • Minneapolis
NEAREST DISTRIBUTOR UPON REQUEST
SOLE PRODUCERS 92 N. J. RAILROAD AVE. NEWARK, N. J.

Bronze-Arc Mn...

the First and Only ELECTRODE for welding Manganese Bronze

This 2000 lb. manganese bronze casting had areas of excessive porosity in the "as cast" condition. Bronze-Arc Mn was selected for the repair because good color match, machinability, and soundness were important. The casting passed X-Ray tests after repair with Bronze-Arc Mn.

...welds
***without* high preheating**
***without* distortion and warpage**

Now you can do it... weld manganese bronze castings, reclaim and repair manganese bronze ship propellers, fabricate manganese bronze assemblies with Bronze-Arc Mn.

Never before could you weld manganese bronze without high preheat and interpass temperatures—but you can with Bronze-Arc Mn. And that eliminates a major cause of distortion and warpage. Bronze-Arc Mn welds have excellent physicals—and the chemical composition is the same as the basic material.

Developing electrodes for hard-to-weld alloys is a job that never ends at Alloy Rods Company. New Bronze-Arc Mn, the first and only electrode for electric arc welding of manganese bronze, is proof of Alloy Rods continuing leadership in research.

Put Bronze-Arc Mn to your own test—order a ten pound container from your distributor today.

OVEN FRESH BRONZE-ARC Mn electrodes are packed in the familiar ALLOY RODS red-and-yellow, hermetically sealed metal containers.

Alloy Rods Co.

General Offices and Plant • York 3, Pennsylvania
Pacific Coast Sales Offices and Plant, El Segundo, Calif.

ARCALOY Stainless Steel Electrodes
BRONZE-ARC Bronze Electrodes
NICKEL-ARC Electrodes for Cast Iron
TOOL-ARC Electrodes for Tools and Dies
WEAR-ARC Hard-Facing Electrodes
WELD-ARC Low Hydrogen Electrodes

no finer electrodes made... anywhere

Get all the Facts... **Fill out this coupon and we'll send you the complete story on the new Bronze-Arc Mn electrodes.**

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Company Name.....

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City.....*State*.....

PERSONNEL

Mikhalapov Joins Coast Metals

Coast Metals, Inc., of Little Ferry, N.J., has announced the election of George S. Mikhalapov as President to succeed John P. Rutherford. The company is one of the leading manufacturers of hard-surfacing alloys and a pioneer in the development of wear-resistant castings.

Mr. Mikhalapov started his career with Stone & Webster upon graduation from MIT in 1926, working on the design and construction of central power stations and industrial plants. While Director of Research and Development for Taylor-Winfield Corp. in Warren, Ohio, prior to World War II, he developed a series of resistance welders which were used extensively to spot weld aluminum alloys for wartime aircraft production.

During the war years he served in the Office of Scientific Research and Development, and directed a number of projects which contributed to the war effort. Some of these projects promoted the development of welded construction for Army tanks and armored vehicles—others determined the causes and prevention of fractures in the welded hulls of Liberty ships.

At the end of the war, Mr. Mikhalapov joined Air Reduction Co., as Manager of their Apparatus Research Department. While in this capacity, he organized and built the Air Reduction Research Laboratories at Murray Hill, N. J.

In 1951 he became affiliated with the Knolls Atomic Power Laboratory of General Electric, directing the engineering development of equipment and processes necessary to apply atomic energy to the generation of power for the nuclear powered submarine, which is now being constructed for the United States Navy.

Mr. Mikhalapov is a member of the AMERICAN WELDING SOCIETY, American Society of Mechanical Engineers and the American Society of Testing Materials. In 1947 he presented the Adams Lecture to the AMERICAN WELDING SOCIETY. He holds a number of patents, mainly for the development of electrical equipment and welding and metal-working processes.

Richardson National Sales and Service Supervisor

L. D. Richardson has been named National Sales and Service Supervisor by the Eutectic Welding Alloys Corp., Flushing, N. Y. His new duties will include supervision of sales, sales training and service in the U. S. and Canada.

This appointment ties in with the corporation's 1954 expansion program and is necessary to coordinate the activities of the various distribution centers being set up in the U. S. and Canada.

Richardson has been with the corporation for the last nine years. Previously he was chief welding engineer for the prime contractor on the U. S. Corps of Engineers, Manhattan Project at Oak Ridge, Tenn., having charge of welding in the Atomic Energy Gaseous Plant, Power House and other AEC construction operations.

His earlier experience includes two years as general foreman of welding at St. Johns River Shipbuilding Corp. in Jacksonville, Fla., and two years as general foreman of welding with W. A. Bechtel Corp., in San Francisco, Calif.

L. D. Richardson is a well-known lecturer on all phases of welding. He has addressed many chapters and sections of the American Society for Metals, AMERICAN WELDING SOCIETY, American Society of Tool Engineers and American Foundrymen's Society. He is a member of the AWS, the ASM and New York Sales Executive Club.

E. R. McClung Made Assistant Manager

Appointment of E. R. McClung as assistant manager of the Alco Products Plant of the American Locomotive Co. at Dunkirk, N. Y., has been announced by E. F. Murphy, manager.

Prior to this appointment, Mr. McClung was assistant manager of Alco's Ordnance Plant in Schenectady. In that capacity he was engaged in the manufacture, testing and shipping of M-47 medium combat tanks destined for the U. S. Army Ordnance Corps and America's allies throughout the western world.

Mr. McClung joined Alco in January 1952 as superintendent in charge of tank hull manufacture and was promoted to night superintendent of the ordnance plant in May 1952. He then became general superintendent of the plant in July 1952, and was made assistant manager in September 1953.

Before joining Alco, Mr. McClung spent six years as plant superintendent of the Lukenweld Division of the Lukens Steel Co. at Coatesville, Pa. During the war years he was welding engineer at the New York Shipbuilding Corp., Camden, N. J. Previously he had been a development engineer for Linde Air Products Corp. and assistant welding engineer at the Brooklyn Navy Yard, New York.

Mr. McClung is a graduate of the U. S. Naval Academy, Annapolis, Md., and is a member of the AMERICAN WELDING SOCIETY, having served as chairman of the Philadelphia section in 1947 and 1948.

Employment Service Bulletin

Position Vacant

V-309. Young mechanical engineer to carry out process development and design of equipment for metal removal. Experience with welding or oxy-acetylene cutting preferable but not essential. An excellent opportunity for the right man to grow with a young, aggressive and rapidly expanding firm. Send brief history and starting salary desired with reply.

Services Available

A-650. Experienced fabricating shop supervisor with good background welding and code practices, desires position as superintendent or equal, with well-organized company manufacturing process equipment, weldments, etc. Interested in new procedures, cost reduction, design and sale of products. Complete résumé available.

CHECK LINCOLN'S NEW LOW PRICES

before you buy any AC welder

FLEETWELDER

... prices reduced 11% to 24%

◀ **Fleetwelder AC** *with Arc-Booster. 300, 400, 500 amp sizes. For wide range of light and heavy welding with AC.*

Fleetwelder Special. ▶ *300, 400, 500 amp sizes. More useable amps* per dollar *than any AC welder.*

FLEETWELDER SPECIAL

... new *low-priced* AC welder

LOWEST COST AC WELDING

Lincoln's new combination of lower AC machine prices and faster welding with Jetweld heralds another development in Lincoln's program to lower welding costs.

The highest manual AC welding speeds ever attained are now available to industry . . . and at lowest possible cost because:

1. Lincoln AC machine prices are lowered 11% to 24% . . . *and more with the new Fleetwelder Special.*

2. 35% and higher welding speeds can now be realized with Jetwelding . . . using the cost-cutting combination of Jetweld and Lincoln AC power.

WRITE FOR PRICES

— Benefit Now. Have your Lincoln representative check your requirements for both electrodes and welders. Call or write.

THE LINCOLN ELECTRIC COMPANY

DEPT. 1904 • CLEVELAND 17, OHIO

THE WORLD'S LARGEST MANUFACTURER OF ARC WELDING EQUIPMENT

NEW LITERATURE

Ampco-Weld Wall Chart

A colorful and most informative wall chart is available for immediate distribution, free of charge, by the Resistance Welding Division of Ampco Metal, Inc., 1745 S. 38th St., Milwaukee 46, Wis.

The $20^1/_2$- x $29^1/_2$-in. hanging chart incorporates a total of nine valuable data charts and ten photographs.

The feature chart in the upper left corner includes the RWMA Specifications for spot welding similar and dissimilar metals. The other eight data charts, of the three-color presentation, cover the average properties of Ampco-Weld, high conductivity alloys, projection welding, conversions on the commonly welded metals, heat balance, secondary amperes and electrode force, seam-welding and spot-welding schedules on low-carbon steel.

The photographs show Ampco-Weld holders, tips, seam Welder shafts and bushings, seam welder wheels, backings, dies and tips.

Electrode Holder

The Duro Engineering Co., Inc., 136 Park St., New Haven 11, Conn., has issued a leaflet describing a new electrode holder which weighs only 16 oz. This electrode holder permits fast changes by lever action, no turning. Such merits include long life, durability and good connections. Parts are readily replaced.

Mechanics of Materials

A book by Philip G. Laurson and William J. Cox has been published by John Wiley & Sons, Inc., 440 Fourth Ave., New York 16, N. Y., and sells for $5.75. Since this book was first published over fifteen years ago, it has won an established reputation both as a reference and text. One reason for its continuing popularity is the fact that the authors never lose sight of their ultimate purpose—providing a clear understanding of the fundamental principles underlying machine and structural design. Welding design is covered.

Professors Laurson and Cox examine the physical behavior of stressed bodies, not merely from the viewpoint of its mathematical expression but also in the light of the practical problems that confront the engineer. They present their material in a way that is easily understood, taking particular care to express statements clearly and support them with ample explanations. Throughout the book they encourage the reader to think of the subject in terms of its engineering realities.

The Third Edition retains much of the outlook of earlier editions. At the same time it has been largely rewritten in the interest of adding to the book's clarity and timeliness. The most important changes are the increased emphasis on the statics of stress determination, and the postponement of the material on stresses determined by deformation. The chapter on combined stress has been rearranged to give increased emphasis to Mohr's Circle. Other changes are: the revision of the tables of structural shapes which have been brought into conformity with present day standards; the inclusion of many new illustrations; and the addition of a whole series of new problems, many of them based on actual, well-known engineering structures.

Welding Process and Procedures

This book of 255 pages by Joe L. Morris was published by Prentice-Hall, Inc., 70 Fifth Ave., New York 11, N. Y.

The author has prepared this book for the express purpose of serving those groups attending colleges that offer brief courses in welding, and for technical institutions and the more advanced vocational schools. An earnest effort has been made to arrange the text material in such a manner that those portions considered to be too advanced for certain students may be omitted without destroying the continuity of the presentation. The practicing engineer and the industrial worker will also find the book useful because of its simplified presentation of welding processes and procedures.

The arrangement relating to the more important welding processes is as follows: (1) History, (2) description, (3) details of use, (4) applications and limitations. Price: $5.00.

Welded Ship Construction

The encounter of two ships at sea can tell a great deal about the strength of materials, the effectiveness of the tools and techniques used in construction and the quality of the skill and craftsmanship.

Air Reduction is offering a new 4-page folder entitled: "Collision Reveals True Story of Welded Ship Construction."

The article, written by J. D. Beery, of Ingalls Shipbuilding Corp., and LaMotte Grover, of Air Reduction, was reprinted from *The Welding Engineer*, It discusses the advantage of welded seams over riveted seams in building ships. When the SS *Loide Panama*, built by Ingalls, was rammed by an oil tanker off Barnegat City, N. J., some months ago, a V-shaped cut was made at the point of collision. A careful study was made of the damaged area and it was found that her riveted seams and joints had parted while all welded seams had held. In the opinion of engineers experienced in the behavior of riveted and welded ship construction, the fact that the welds held probably saved the ship from sinking.

For complete details with pictures of the damage, request a copy of this folder by writing to Air Reduction Sales Co., 60 E. 42nd St., New York 17, N. Y.

U. S. Air Force Machinability Report

This book of 153 pages, the third volume in a series of studies on machinability, was prepared by the Curtiss-Wright Corp. with the U.S. Air Force. It is hoped that increased production and reduced costs will be obtained through a better understanding of the machining process and control of materials, tools and machines. The book deals with titanium and its alloys.

Ferrous Process Metallurgy

This book of 414 pages, by John L. Bray, was published by John Wiley & Sons and sells for $6.50. It covers the following subjects: raw materials, the iron blast furnace, wrought iron, the Bessemer Process, the Basic Open-Hearth Process, the Acid Open-Hearth Process, electric furnaces, ingots and ingot molds and index.

Flame Cutting

The Arcair Co., of Lancaster, Ohio, and Bremerton, Wash., has announced the recent acquisition of the manufacturing and distribution of the Meldrum Burner's Aid, formerly manufactured by the Meldrum Engineering Co., of San Francisco, Calif.

The improved model of the Meldrum Burner's Aid, a tool used to adapt gas torches for accurate cutting of circles, bevels and contours, is now being produced by the Arcair Co. and will be known and advertised as the Improved Burner's Aid.

The Arcair Co. also announces the avail-

ability of a free booklet explaining the use of the Improved Burner's Aid for those interested in precision cutting with flame torches. Requests for this booklet, *Fundamentals of Flame Cutting*, should be sent to Arcair Co., PO Box 337, Lancaster, Ohio, or PO Box 4227, Bremerton, Wash.

"Forging Ahead"

The American Car and Foundry Co., 30 Church St., New York 8, N. Y., has issued a deluxe pamphlet entitled "Forging Ahead with ACF." This pamphlet gives a complete story of the industrial forging service of the company. One section is devoted to welding. Copies are available on request.

Gas Drive Welder

The Hobart Brothers Co. offers a new 48-page book titled *How to Build Your Own Gas Drive Welder*.

This book takes the reader through a progressive step-by-step plan, showing actual photographs, plans and ideas for the welding operator who wishes to build his own portable gas drive arc welder.

Ask for EW-183, available at no charge from The Hobart Brothers Co., Troy 1, Ohio.

Alloy Reference Chart

A new Alloy Reference Chart listing the chemical analyses, physical properties and recommended applications for 28 different grades of cast stainless steel has been made available by the Cooper Alloy Foundry Co., Hillside, N. J. Prepared by Norman S. Mott, Chief Chemist and Metallurgist, the bulletin indicates ACI, AISI, SAE, ASTM and general "type" designations. Data as to heat treatment and remarks concerning weldability and machinability are included. Copies are available on request.

Codes and Standards

Volume One, Number Two, of the *Taylor Forge Magazine* includes interesting items of importance to those concerned with Codes and Standards dealing with piping and pressure vessels. A list of the articles are: Keeping Up to Date on Codes and Standards Dealing with Piping and Pressure Vessels, Review of ASA B16.5—1953, Steel Pipe Flanges and Flanged Fittings, Pressure-Temperature Ratings, Class A and Class B Facings—150 to 2500 Pound, Appendix D, Method of Rating Ferrous Alloy Steels, Report of Task Force on Flexibility, and ASA Code for Pressure Piping.

This publication is available upon request of the Taylor Forge and Pipe Works, PO Box 485, Chicago, 90, Ill.

Page Welding Wire

Now—a new line-up of PAGE welding wire in coils, reels, packages

• Here are some of the ways PAGE welding wires are packaged to protect them during shipment and to make it easy for you to handle and stock them. Two new packages are Leverpaks and pallet-mounted coils.

LEVERPAKS • Here's one of the best containers for coils up to 16″ ID. It's light-weight, but durable. It is sealed air-tight and is packed with moisture-absorbing substance to reduce corrosion. Release safety catch, pull lever, and the whole top comes off. It's just as quickly resealed. Leverpaks can be rolled easily. They stack perfectly and take a minimum of floor space. They cost you nothing extra and have many re-uses.

PALLET-MOUNTED COILS • If you have a fork-lift truck, you'll like PAGE'S new pallet-mounting of 22″ ID coils. Steel strapping and paper wrapping protect coils and hold them securely. You can run them in and out of freight cars and trucks, and spot them in your plant.

AUTOMATIC WELDING WIRES • **Inert Gas**—Six PAGE-ALLEGHENY stainless grades in .035″, .045″, and .0625″ diameters. Precision thread-wound on 25-lb. non-returnable reels to fit popular arc welding machines. **Submerged Arc**—PAGE Stainless in wire diameters from 1/32″ to 5/16″, plain or copper coated. In layer-wound coils or 22″ or 24″ mill coils.

GAS WELDING RODS • In a variety of analyses: stainless, Armco, medium and high carbon, and manganese or naval bronze.

Warehouse stocks in Chicago, Denver, Houston, Los Angeles, San Francisco

Write **our Monessen, Pa., office for literature and prices**

ACCO

Page Steel and Wire Division
AMERICAN CHAIN & CABLE

Monessen, Pa., Atlanta, Chicago, Denver, Detroit, Los Angeles, New York, Philadelphia, Portland, San Francisco, Bridgeport, Conn.

GAS Automatic Arc

M & T Catalog

Metal & Thermit Corp. announces publication of a new, fully illustrated 20-page catalog of arc-welding accessories. The M&T accessories line of high quality aids to good welding includes electrode holders, helmets, eye shields, ground clamps, cleaning tools, gloves, welding cable and many other related products.

M&T accessories are offered through the company's six district sales offices as well as through some three hundred welding distributors located throughout the country. Copies of the new catalog may be obtained by writing to Metal & Thermit Corp., 100 E. 42nd St., New York 17, N. Y.

Welding and Flame-Cutting Apparatus

National Welding Equipment Co., of San Francisco, Calif., has released a new, 36-page, welding and flame-cutting apparatus catalog. This new catalog lists all of the major and most frequently needed equipment items. Deliberately, this manufacturer refrained from making this catalog cumbersome to read by excluding many special types of torches, regulators, tips and nozzles. The catalog does, however, list and illustrate in a clear, concise manner, all of the equipment the average user of welding and flame-cutting apparatus will require.

Also listed and illustrated are a number of detail information brochures which are more fully descriptive of design and construction details and operating suggestions. An interestingly handled and easily readable price list is included with the catalog.

Aluminum and Steel Cleaner

Oakite Products, Inc., manufacturers of specialized cleaning and allied materials, have announced the development of Oakite Composition No. 161, an alkaline-type material designed to clean aluminum, steel and other metals in pressure-spray washing machines without objectionable foaming.

Oakite Composition No. 161, the manufacturers state, is a white, free-flowing, powdered material that is completely soluble in water, free rinsing, and inhibited to provide safety to aluminum.

Readers desiring additional information, without charge, regarding this new material, should address Oakite Products, Inc., 118E Rector St., New York 6, N. Y.

Resistance Welding of Dissimilar Metals Bulletin Just Released

A new bulletin in the series issued by the Resistance Welder Manufacturers' Assn. entitled, *Resistance Welding of Miscellaneous and Dissimilar Metals*, has just been printed.

This bulletin, No. 19, covers information on the spot, seam and flash welding of titanium, cobalt-base alloys and refractory metals, as well as zinc and zinc die castings, lead and lead alloys and dissimilar metals.

The bulletin contains a number of tables and photographs.

Copies are obtainable at the Headquarters of the Resistance Welder Manufacturers' Assn., 1900 Arch St., Philadelphia 3, Pa., and the cost is ten cents each.

Induction and Dielectric Heating

This valuable book by J. Wesley Cable, Consulting Engineer, offers a carefully organized treatment of the latest uses and applications of high-frequency heating in industry. Divided into two major sections, dealing with induction and dielectric heating, respectively, the book contains sufficient theory to enable the reader to understand fully the basic phenomena applicable to both methods.

The book consists of 576 pages and is published by the Reinhold Publishing Corp., 330 W. 42nd St., New York 36, N. Y., and sells for $12.50.

One chapter deals with "Brazing and Welding by Induction Heating"; another chapter deals in "Soft Soldering by Induction Heating."

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Effective March 1, 1953	1 time	3 times	6 times	12 times
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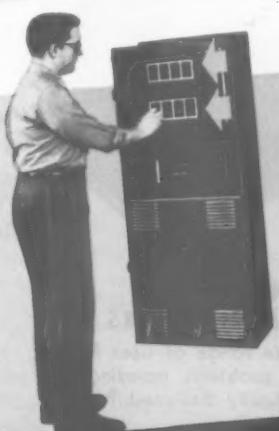

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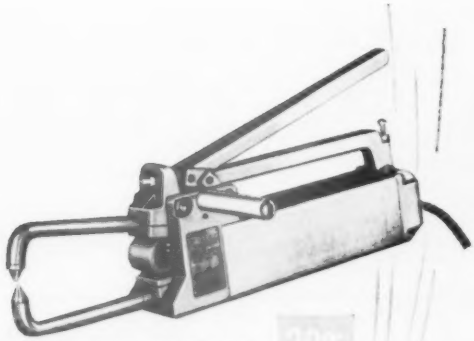

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abstracts of
CURRENT WELDING PATENTS

prepared by V. L. Oldham

Printed copies of patents may be obtained for 25c from the Commissioner of Patents, Washington 25, D. C.

2,670,789—Acetylene Torch with Angularly Adjustable Tip—Charles Dieterich, Island Park, N. Y.

This patent covers a specific construction wherein a movable head section is provided and means are provided in the torch to hold the movable head section releasably in a desired position with relation to the fixed head section of the torch.

2,671,155—Resistance Welding Apparatus—Edgar Lees and Frank Arthur Cooke, Loughborough, England, assignors to Rolls-Royce Ltd., Derby, England, a British company.

This patent relates to stitch welding apparatus and cooperating rotatable wheel element welding electrodes are provided with one welding electrode being fixedly positioned and the other one being carried by a movable arm to move the electrodes relatively toward and from one another. Means are provided for effecting a step-by-step rotation of the electrodes and other means load the welding electrodes to apply welding pressure thereto in alternation with the step-by-step rotation therof.

2,671,195—Polyphase Welding System With Transfer Transformer—Fred W. Jaksha, Chicago, Ill., assignor to Welding Research Inc., Chicago, Ill., a corporation of Illinois.

Jaksha's patent relates to a welding transformer having three primary windings on the iron core of the transformer and connected to each other in delta, and circuit means connecting the winding to its respective phase of a three-phase alternating current source. A welding circuit is provided and includes a secondary winding on the iron core having at least one electric discharge valve in series circuit relation with each primary winding for controlling unidirectional current flow through the winding. A transfer transformer is electrically connected with the welding transformer to improve the operating characteristics of the welding transformer and increase the value of the welding current. The transfer transformer has a separate iron core with three windings only on its core and the electrical connection of the transfer transformer is such that the windings of such transformer are in parallel relation to a respective primary winding on the welding transformer.

2,671,196—Welding Transformer—Frederick C. Owen, Fayetteville, N. C.

An alternating-current arc welder is disclosed in the patent and it includes a shell-type core and primary and secondary windings on the center leg of the core with at least part of the secondary windings axially spaced from the primary windings. Magnetic inserts span the distances between the center and side legs of the core and are movable into and out of positions between the spaced windings on the center leg of the core. A resonant circuit, reactive on the windings, has its resonant frequency dependent on the setting of the magnetic inserts.

2,671,501—Cutting Torch Tip—Daniel A. Marra, Cheswick, Pa.

Marra's patent relates to a blow-pipe tip including an elongate tubular body made from metal of relatively low strength and durability when exposed to steel at fusing temperature in the presence of oxygen and having a sintered carbide shoe on the delivery end of such tubular body. This shoe has relatively great durability when exposed to steel at melting temperature in the presence of oxygen; special baffle and gas passage bores are provided in the torch tip.

2,671,843—Electric Welding—Richard B. Steels, New Providence, N. J., assignor to Air Reduction Co., Inc., New York, N. Y., a corporation of New York.

This patent discloses and covers alternating-current arc-welding apparatus including a welding electrode, a welding circuit connected to the electrode and to a workpiece for supplying alternating welding current to maintain the welding arc. Other means are present for adjusting the welding position of the electrode with respect to the workpiece, and means responsive to selected stable half cycles of the arc voltage are present in the apparatus for actuating the adjusting means to maintain the arc length substantially constant.

2,671,846—Means for Inductively Heating Narrow Elongated Portions of Cylindrical Bodies—Alfred C. Body, Cleveland, Ohio, assignor to The Ohio Crankshaft Co., Cleveland, Ohio, a corporation of Ohio.

Body's patent is on a high-frequency inductor past which an axially split tube is to be continuously moved to have its edges adjacent the split progressively heated to a welding temperature. This inductor comprises a main conductor, a pair of side conductors one on each side of the main conductor and symmetrically disposed relative thereto, and a pair of end conductors extending transversely to and electrically connected to respective ends of the main and side conductors to connect the conductors in an electrical parallel circuit. Workpiece-facing surfaces provided on the main conductor and side ductors form a generally inverted, U-shaped channel through which the tube passes and which channel is substantially greater in diameter than the diameter of the tube.

2,671,869—Automatic Arc Welding Method and Apparatus—Richard B. Steel, New Providence, N. J., assignor to Air Reduction Co., Inc., Murray Hill, N. J., a corporation of New York.

This patent relates to the structure covered in patent number 2,671,843 and adds thereto means for maintaining the arc voltage at a normal value. Such means include a voltage divider connected parallel to the arc, a capacitor connected across one section of the voltage divider and charged when the welding circuit is energized and before the arc is established, and means responsive to the capacitor charge and to the discharging of the capacitor after the arc is established for counteracting the effect on the electrode-moving means of the increment of arc voltage above said normal value existing during the interval when the electrode is heating up.

2,671,952—Method of Welding Brake Beams—Walter H. Baselt, Chicago, Ill., and Loren L. Whitney, Hammond, Ind., assignors to American Steel Foundries, Chicago, Ill., a corporation of New Jersey.

This patent relates to a special welding operation particularly adapted for making truss-type brake beams. Registering slots are provided in abutted walls of two sections of the brake beam and a welding portion is positioned to substantially

close one opening of the registered slots. A bifurcated chill is positioned against a portion of the members to be welded together and simultaneously the walls and welding portion are welded together at the registered slots.

2,672,187—Torch Tip Mounting—Elmer H. Smith, Minneapolis, Minn., assignor to Smith Welding Equipment Corp., Minneapolis, Minn., a corporation of Minnesota.

This patent is on a specialized torch tip wherein the novelty particularly relates to a provision of an easily removed torch tip carried in leaktight engagement with the body of the torch by a hand-operated clamping member.

2,672,543—Welding Heat Control—Donald P. Faulk, Lexington, Mass., assignor to Raytheon Manufacturing Co., Newton, Mass., a corporation of Delaware.

This patent is on a method of welding comprising urging the parts to be welded together, producing an alternating electric current in the region of the junction of the parts and varying the intensity of the current for a plurality of cycles. Each cycle varies in intensity from a minimum value that exists within only one period of the alternating current and then returns within one period to the minimum value.

2,672,544—Apparatus for Welding by Means of Electromagnetic Induction Heating—Lazare Finchelstein, Paris, France, and Antoine Rodolphe Baffrey, Liege, Belgium, assignors to Societe Marocaine Technique et Commerciale, a corporation of Morocco.

This patent is on an induction welding apparatus for butt welding two adjacent edge portions of electrically conductive material. The apparatus comprises in combination an elongated magnetically permeable means having an elongated face formed with a groove extending along the length of such means so as to form two elongated pole faces separated by the groove. This elongated means is adapted to be disposed lengthwise of the edges to be welded with the edges located opposite the groove. An induction coil including a conductor portion extends lengthwise in the groove in heat-inducing relationship to the edge portions of the material. Supply conductor portions are located outside of the groove closely adjacent to the magnetically permeable means. Means supply alternating current to such coils.

2,672,838—Brazing Machine—Albert E. Heidenreich, Maywood, and Robert C. Hoffman, Chicago, Ill., assignors to Victor Adding Machine Co., Chicago, Ill., a corporation of Illinois.

This patent upon a brazing machine includes the combination of means comprising a movable wire to feed the brazing wire along a predetermined path and a normally stationary work holder to support the workpiece. Other means support the wire grip for reciprocating movement toward and away from the workpiece, a spring engages the wire grip to move it, and motive means engage the spring to stress the spring intermittently for moving the wire grip toward the workpiece and for holding the end of the wire against the workpiece. The grip advances the wire as rapidly as it is melted and other means return the wire grip after the stress on the spring is relaxed.

2,673,063—Method and Device for Pulling Electrodes—Harry Weinman, Detroit, Mich.

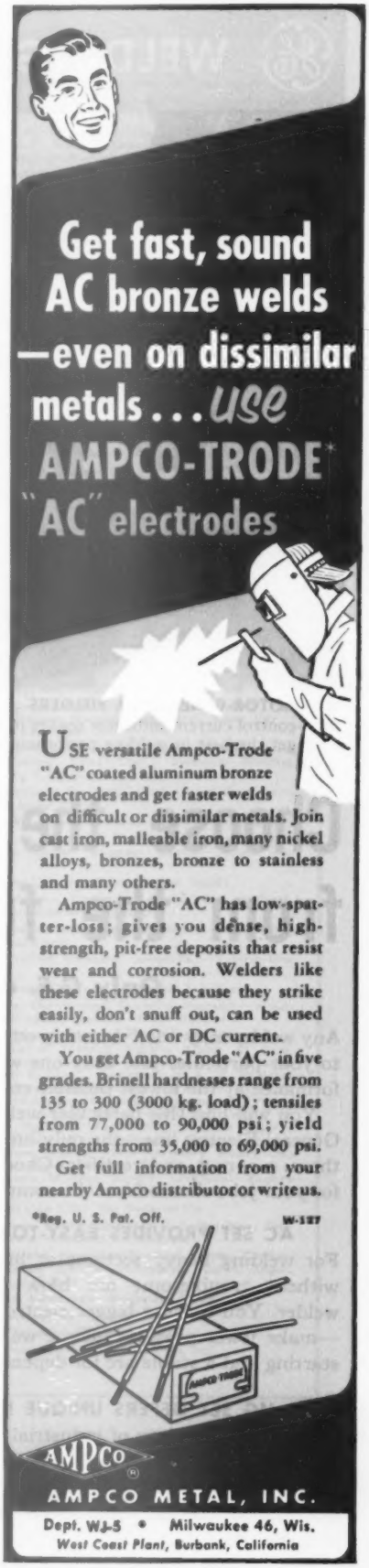
This device relates to a tool for prying a welding electrode from its seated engagement with an electrode holder. A lever is provided in the device and has a sharp transverse prying edge with the lever being pivoted on a support to swing the prying edge forcefully away from the holder to cut into the electrode before the center portion of a prying edge engages the electrode in a prying operation.

2,673,274—Strip Heating—John T. Vaughan, Hudson, and James W. Williamson, Cleveland, Ohio, assignors to The Ohio Crankshaft Co., Cleveland, Ohio, a corporation of Ohio.

The patent relates to a method of progressively heating continuous lengths of strip material for forming and welding of the edges and the method comprises radiating heat uniformly along the strip while in the flat state to heat into a hot-forming temperature and subsequently flowing high-frequency currents in the edges of the strip while still in a flat state to heat the edges to a plastic forge-welding temperature. Next the flat strip is formed into a tube and the edges are brought into pressure-welding engagement.

2,673,275—Electrically Welding Sheet Metal Container—Helmut Kreft, Braunschweig, Germany.

Kreft's patent is on an electric resistance welding process for continuous manufacture of overlapping longitudinal seams in metal packings and metal containers. The method comprises pressing together overlapping parts of the seam to be welded throughout the entire length of the weld solely by a pair of electrodes at a sufficiently high surface pressure, one electrode being positioned above and the other below the overlapping parts, and causing a single brief current pulse sufficiently strong to weld the seam to pass through the seam throughout the whole of its length only after the desired pressure has been applied to the overlapping parts. Electrodes are separated from each other after the current supply to the electrodes has been completed and then the electrodes are rotated through a certain angle after each welding action.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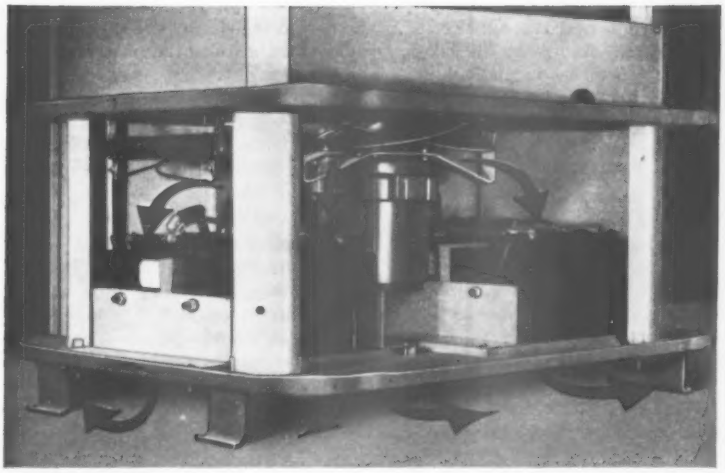

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Wisconsin: Milwaukee—Machinery & Welder

Alaska: Anchorage—Northern Supply

Canada: Toronto—Canadian G.E.

Hawaii: Honolulu—American Factors, Ltd.

GENERAL ELECTRIC

2,673,276—Postheating of Electrically Welded Tubes in Line of Production—Ernst W. Allardt, Alliance, Ohio, assignor by mesne assignments, to the Babcock and Wilcox Co., Jersey City, N. J., a corporation of New Jersey.

In this process, immediately after the formation of a welded seam in a welded tube, the tube is progressively heated before sizing and shaping the tubing to a temperature of substantially 100° F above the critical range of the steel strip. Thereafter the tubing is rapidly cooled while passing through a controlled neutralizing atmosphere after which the tubing is progressively sized and shaped and cut to preselected lengths.

2,673,278—Method of Electric Welding—Nelson E. Anderson, Berkeley Heights, N. J., assignor to Air Reduction Co., Inc., a corporation of N. Y.

Anderson's welding method comprises placing a gas discharge end of a gas nozzle in contact with a workpiece so that it substantially surrounds a selected spot on the surface of the workpiece and a shielding gas is flowed through the nozzle onto the surface spot to exclude air therefrom at which time the electrode is advanced into electrical contact with the surface to initiate the flow of arc welding current. Thereafter the electrode is released and the electrode is retracted to create a gas-shielded welding arc within the nozzle between the electrode and the workpiece and the flow of the arc welding current is terminated at a selected time interval following initiation of the arc.

2,673,333—Rotary Electrode Assembly—Melvin M. Seeloff and Joseph H. Cooper, Warren, Ohio, assignors to The Taylor-Winfield Corp., Warren, Ohio, a corporation of Ohio.

This patent relates to a special rotary electrode assembly including a housing, spaced antifriction bearings in the housing and a current conductive spindle journaled by the bearings. Contact means are present for transferring electrical current from the housing to the spindle and the patent relates to special details of the assembly.

LIST OF NEW MEMBERS

Effective February 28, 1954

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B—Member
C—Associate Member
D—Student Member
E—Honorary Member
F—Life Member

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Dawson, B. L. Jr. (B)
Kelley, T. D. (B)

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Durant, George A. (B)
Hanbery, Donald T. (C)
Helzner, Albert E. (C)
Lawrence, Robert R. (B)
Minichiello, John J. (C)
Sweeney, Edward F. (B)

CHATTANOOGA

Jones, J. Paul (B)
Underwood, James J. (B)

CHICAGO

Borto, Edward J. (C)
Brown, Richard O. (C)
Cislaghi, John R. (C)
Haas, William C. (C)
Harris, Charles L. (C)
Kemp, John C. (C)
Kukolich, Joseph (C)
Larson, Alfred J. (C)
Smolek, Mack J. (C)

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Dickert, Albert E. (B)
Martin, Gayle W. (B)
McDaniel, C. Leslie (B)

CLEVELAND

Barber, A. W. W. (B)
Buyers, Theodore B. (C)
Dixon, Ralph (A)
Dufala, Steve M. (C)
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Swan, Harry O. (B)

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COLUMBUS

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Kurtz, Paul (B)
Nelson, Jerome (D)
Van Horn, Donald J. (B)

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Morrison, George M. (C)

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Head, Herberth E., Jr. (C)
Palmer, Herbert O. (B)
Rexin, Floyd R. (B)
Schemansky, Irvin E., (B)
Smith, Herbert L., Jr. (C)
Waggener, Max H. (C)
Wyte, Charles (A)
Young, Walter P. (C)
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EAST TEXAS

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HOUSTON

Beard, George E. (C)
Boyd, G. W. (C)
Brandt, E. F. (B)
Butler, James H. (B)
Hester, Henry L. (B)
Jones, Jesse R. (B)
Ketchbaw, Thomas E. (B)
Lamb, Raymond O. (C)
Massey, Clem M. (C)
McClelland, H. A. (C)
Mills, Maurice E., Jr. (B)
Prassler, Alton H. (C)
Reeves, Delbert (B)
Riley, Walter A. (B)
Ritter, Harry A. (B)
Scurlock, S. W. (B)
Snyder, R. T. (C)
Wynne, M. V. (C)
York, W. Wesley (B)

INDIANA

Miller, C. J. (C)

IOWA

Johnson, Harry M. (B)

KANSAS CITY

Hoffmann, John B. (B)
Roach, John M. (B)

LEHIGH VALLEY

Couch, M. F. (C)
Daden, Henry A. (C)

LOS ANGELES

Bisch, Raymond A. (C)
Crawford, Gerald D. (C)
Kraus, William P. (C)
Moir, Norman C. (C)

MAHONING VALLEY

Mac Kusick, Meredith H. (A)

MARYLAND

Craig, William J. (C)
Outerbridge, John J. (C)
Philibert, B. J. (B)

MILWAUKEE

Harder, Robert Christian (B)
Johnson, Gordon S. (C)
Seitz, Carl K. (B)

NASHVILLE

Tobey, Frank T., Jr. (B)

NEW JERSEY

Arbizzani, John D. (B)
Diller, Charles (C)
Jones, T. A. (B)
Lancaster, John E. (C)
Portik, Frank (C)
Premo, Fred C., Jr. (C)
Shanley, Joseph B. (C)

NEW YORK

Aguilar, Joseph (C)
Barto, Howard S. (B)
Briggs, Clyde W. (B)
Davis, Harold S. (C)
Delbridge, Chester F. (B)
Ferrentino, Edward A. (C)
Maxey, Arthur J. (C)
Murphy, James L. (B)
Platz, Robert M. (B)
Ressa, Pat (C)
Rogers, William W. (B)
Roth, Lawrence (B)
Steck, Paul C. (B)

NIAGARA FRONTIER

Brown, Chuck (B)
Fessel, Kenneth (B)
Grieser, Harold W. (C)
Kieffer, Raymond J. (B)
Poteat, Laurence E. (C)
Prieur, Jerome H. (B)
Stadler, Raymond A. (C)
Stevenson, Henry (B)

Watts, Charles (B)
Zech, W. (B)

NORTHEAST TENNESSEE

Goforth, Johnnie A. (B)
Guthrie, R. H. (B)
Hardy, Charlie E. (B)
Humphries, Jean Willard (B)
Oglesby, Clyde T. (B)
Poole, E. B. (B)
Potts, Harry E. (B)
Tedder G. A. (B)

NORTHERN NEW YORK

Rafferty, Lawrence M. (C)
Wessels, John C. (C)

OKLAHOMA CITY

Fowler, John (C)

OLEAN-BRADFORD

Morris, Byron S. (C)

PASCAGOULA

Fuller, Roland W. (C)
Martin, C. B. (C)

PHILADELPHIA

Bromley, George (B)
MacGregor, Robert R. (C)
Sagan, Stanley S. (C)

PITTSBURGH

Carioto, A. F., Jr. (C)
Cooper, F. R. (B)
Hutchinson, W. R. (B)
Metcalfe, John W. (B)
Schuster, R. H. (C)
Zdrale, Walter F. (B)

PUGET SOUND

Beecham, Robert (B)

RICHMOND

English, Ernest E. (C)
Ramsey, Frank E., Jr. (C)
Sandero J. L. (C)

ROCHESTER

Head, Richard J. (B)
Nudd, Fred A. (C)

SAGINAW VALLEY

Nelson, Elmer E. (B)

SALT LAKE CITY

McClellan, Hugh D. (B)
Taylor, T. J. (B)
Toone, George W. (B)
Winters, Dean G. (B)

SAN FRANCISCO

Alm, Robert D. (D)
Augsburger, J. A. (D)
Bartley, John E. (D)
Chiappino, George Joseph (D)
De Geus, Richard (D)
Fuller, Wallace (D)
Hatcher, Frank L. (D)
Hughes, John W., Jr. (D)
Leavitt, Frank V. (C)
Parker, Ralph L. (B)
Schield, James H. (D)
Schmid, Carl T. (D)
Schumacher, Gerald H. (D)
Stone, W. J. (D)
Turk, Richard Watrous (D)
Van Deventer, John Arthur (D)
Walker, Tom (D)
Yale, Merrill F. (C)

SANGAMON VALLEY

Golembeck, William Jr. (C)
Jolly, Jesse F. (C)
Peters, Howard (C)
Randolph, Paul (C)
Redding, James (C)
Reinholt, William (C)

SUSQUEHANNA VALLEY

Yurkoski, Frank R., Jr. (C)

SYRACUSE

McGowan, Roy B. (C)
Schmeidicke, Karl C. (C)

TOLEDO

Growth, Robert R. (C)
Priest, Russell A. (C)
Spresser, Paul M. (C)
Stowers, Ronald R. (C)

TRI-CITIES

Phillips, William H. (C)

TUCSON

Smith, Richard L. (B)

TULSA

Christopherson, R. H. (B)
Looney, R. L. (B)
Robertson, Moin M. (B)

WASHINGTON

Lyons, Robert E. (C)
Starks, John F. (B)

WESTERN MICHIGAN

Bethke, Edward E. (B)
Francisco, Marvin O. (C)

WICHITA

Fillmore, Ralph (B)
Holmes, Douglas (B)
Ralston, Glen (C)
Stanfield, R. F. (B)

YORK-CENTRAL PENNSYLVANIA

Dietz, Nevin B. (B)
Hessemer, Fritz M. (B)

NOT IN SECTIONS

Arp, Marvin G. (B)
Brillie, Jean (C)
Butler, Charles (B)
Calvo, Rafael (B)
Hancock, Leslie John (B)
Happe, Willem Fredrik (C)
Herpol, George A. (B)
Garritsen, Willem (C)
Granjon, Henry (C)
Kadlez, Karl (C)
Kihn, Nicolas (C)
Landragin, Gustave Paul (C)
Lundqvist, Svend (C)
Perego, Giovanni Paolo (C)
Mantel, Wilhelm (B)
Speth, Karlheniz (B)
Thomas, John Reginald (B)

Members Reclassified

During the month of March

CINCINNATI

Foster, George (C to B)

NEW YORK

Jungbluth, Josef (C to B)

NORTHERN NEW YORK

Domina, Frederick H. (D to C)

OKLAHOMA CITY

Barnes, Marvin J. (C to B)

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SECTION NEWS AND EVENTS

as relayed to C. M. O'Leary

Welding Structural Steel

Bethlehem, Pa.—An attendance of 150 heard a discussion on "The Welding of Structural Steel" by Carl L. Kreidler AWS, of the Lehigh Structural Steel Co., at the March 1st dinner meeting of the *Lehigh Valley* Section, held at Walp's Banquet Hall in Allentown. Mr. Kreidler used slides and motion pictures to illustrate his talk.

Selenium Rectifier

Boston, Mass.—The March meeting of the *Boston* Section was held on Mar. 8, 1954, at the Westinghouse Auditorium, Boston, Mass. After liquid refreshments and the usual excellent dinner, about ninety members and guests heard Robert R. Brunn of the *Christian Science Monitor* speak on "Interpreting the News."

After the Coffee Speaker, Charles H. Jennings AWS, Chief Engineer of the Welding Section, Westinghouse Electric Corp., spoke on the "Development of the Selenium Rectifier Electric Arc Welder," with the accompaniment of slides showing welders from the beginning of electric arc welding up to the present time. The high spot of this interesting talk was the showing of ultra-high-speed movies of the actual behavior of the arc, in both the flat and overhead positions.

Chairman Brandt announced at this time that there will be another outing at the South Shore Country Club on the 24th of May, similar to the one in 1953, which was so well received by the Section.

Welding Titanium Alloys

Buffalo, N. Y.—John J. Chyle, Second Vice-President AWS and Director, Welding Research, A. O. Smith Corp., was the guest speaker at the March 25th dinner meeting of the *Niagara Frontier* Section, held at the LaSalle Yacht Club. Mr. Chyle's semitechnical talk on "Practical Aspects of Welding Titanium Alloys" covered new applications and methods which were shown on slides. An attendance of 97 were present at the meeting and 86 at the dinner.

Submerged Arc Welding

Chicago, Ill.—A very successful meeting of the *Chicago* Section was held on March 19th. The dinner and social period held at Burke's Grill was attended by 64 members and guests.

The meeting held at the Peoples Gas Light and Coke Co.'s Auditorium starting at 7:30 P.M. was attended by 140. The speaker was Norman Schreiner, AWS, of the Linde Air Products Co., who spoke on "Submerged Arc Welding." He discussed the electrical hookup necessary for single, double and triple electrode welding on one joint. The results obtained by varying the number of electrodes were brought out. Another point of his presentation was to show how, by the addition of chrome and molybdenum in the flux, a change in the weld metal analysis could be brought about. Mr. Schreiner gave his usual excellent presentation and showed his thorough knowledge of the subject which was further demonstrated when he answered questions from the floor following his talk.

Preceding the speaker of the evening, the movie entitled "Getting There Is Half the Fun" was presented. This is a most excellent movie and was enjoyed by all. Unlike most travel films which quickly convey one to a destination and then show some country, this film takes one from the docks in New York, then aboard the *Queen Elizabeth* and lands one in France or England. It shows life as lived while crossing the Atlantic aboard the Cunard's superliner.

Thomas Swisher, Chairman of the Chicago Section, announced the resignation of John Savage, Linde Air Products Co., as Second Vice-Chairman and that he had appointed, upon the recommendation of the Nominating Committee, Robert R. Lincoln, Air Reduction Sales Co., to fill Mr. Savage's unexpired term. Mr. Lincoln is Chairman of the Chicago Section 1954 Golf Outing which will be held at Nordic Hills Country Club on June 12th.

Welding Advances with Aluminum

Cincinnati, Ohio.—In an effort to increase the attendance at the meetings, the officers of the *Cincinnati* Section were requested to secure at least five members apiece and bring them to the dinner. As further inducement, one of the members secured the services of a network TV artist and her assistants and they attended the dinner as guests of honor on Tuesday, March 23rd, held at the Engineering Society Headquarters. The presentation of an honorary membership in the Cincinnati Section, for Miss Doty Mack, Pantomine Hit Parade Star, was the highlight of the dinner meeting.

W. M. Rogerson of Alcoa Process Development Laboratories, New Kensington, Pa., gave a talk on "Welding Advances with Aluminum." He covered the application of brazing (torch, dip and air), oxy-acetylene, oxy-hydrogen, metallic stick welding, resistance welding (spot, seam, flash), solid phase welding, inert tungsten arc, inert consumable electrode (both manual and automatic) and finally the newest method, that of pulling the wire to the gun (filler arc). His talk was illustrated with slides and supplemented with a color movie which showed the latest development of consumable and nonconsumable electrode welding.

At the conclusion of the lecture Mr. Rogerson set up many samples of brazed and welded joints as well as most of the Research Supplements of THE WELDING JOURNAL on welding and forming of aluminum.

To bring the evening to a climax and obtain further publicity for the Section, the officers of the Section were guests on the TV Program "Pantomine Hit Parade."

The Section was very gratified and justly proud that of a membership of 91, they had 56 for the dinner meeting and 88 for the lecture.

Annual Ladies' Night

Cleveland, Ohio.—Over 150 members and guests celebrated the annual Ladies' Night meeting of the *Cleveland* Section. Included in the evening's program were dinner, favors, entertainment and dancing, all taking place at the Allerton Hotel on February 10th. Shown in the picture (on the next page) are four past chairmen of the Cleveland Section. They are (left to right) Frank Flocke, Ross Yarrow, Mike Shane and John Austin and their wives. In the center background are Harold Blum and Bill Mayor, members of the Board of Directors, and their wives.

Production Welding

Cleveland, Ohio.—The regular monthly dinner meeting of the *Cleveland* Section for March was held on the 10th in the Mather Room of the Hotel Allerton with an attendance of 85 at dinner and 95 at the meeting. "Automation for Welding Equipment" was the subject of a talk given by J. R. Wirt AWS of the Delco-Remy Division, GMC, Anderson, Ind. Mr. Wirt, who is a welding engineer in the Process Department, discussed developments in welding machines, controls and

Cleveland Annual Ladies Night

tooling. The main part of his talk covered handling equipment and the coordination of welding equipment with other equipment to reduce handling.

Coffee speaker, George Campbell, of the Public Relations Department, Cleveland Electric Illuminating Co., gave an impressive talk on his experiences before regaining his eyesight after having been blind for 18 years.

Selection of Proper Electrodes

Dallas, Tex.—"The Selection and Use of Proper Electrodes" was the subject of a technical paper presented by E. B. Lutes AWS, of the Arcrods Corp., at the January 27th meeting of the *Dallas* Section, held at the Torch Restaurant. Mr. Lutes used slides to illustrate his excellent talk.

A special feature of the meeting was the showing of a sound color film entitled "Highlights of Southwest Conference (Football)," through the courtesy of the Humble Oil and Refining Co. of Dallas.

Quality Control

Dallas, Tex.—Following a steak dinner at the Delta Restaurant, 48 members of the *Dallas* Section heard an excellent paper on the subject "Shop Inspection and Quality Control of Welding" as presented by D. L. Shall, AWS of the Humble Oil and Refining Co., Baytown, Tex.

Mr. Shall's talk was supplemented by an excellent sound color film entitled "Welding at Baytown" which was shown through the courtesy of the Humble Oil and Refining Co.

Resistance Welding

Dayton, Ohio.—The March meeting of the *Dayton* Section was held on the 8th at the Dayton Engineers' Club. Clarence D. Shultheis AWS, Welding Engineer, Frigidaire Division of GMC, gave a brief history of welding at Frigidaire in order to show how the increased production schedules in past years have been paralleled by an increased application of resistance welding. Slides were used to illustrate the many applications used at Frigidaire.

A film entitled "Operations Blue Jay" describing the building of the air base at Thule, Greenland, was shown through the courtesy of the U. S. Army.

Design for Silver Brazing

Denver, Colo.—Alden W. Swift AWS, of Handy and Harman, N. Y., was the guest speaker at the March 4th dinner meeting of the *Colorado* Section, held in the Dutch Room of the Adams Hotel. Mr. Swift's semitechnical talk on "Design for Silvery Alloy Brazing" was illustrated with motion pictures.

Before the meeting, a film entitled "Welding of Aluminum" was shown through the courtesy of the Aluminum Company of America.

Copper and Titanium

Des Moines, Iowa.—J. J. Chyle, 2nd Vice-President AWS, and Director of Welding Research, A. O. Smith Corp., Milwaukee, spoke before the March 18th dinner meeting of the *Iowa* Section held at the New Pastime Club. Mr. Chyle's topic covered the "Welding of Copper and Titanium" metals. By means of many slides and factual accounts, he enumerated the story of these metals from their early history to present-day usage in the welding field.

Beginning with the method of smelting and refining, Mr. Chyle explained the basic processes involved and the end products or grades of metals available. The mechanical and physical properties along with recommended process charts were shown in detail and explained to the many viewers.

The equipment, types of electrodes, types of gases and types of welding current were also emphasized along with the exact welding procedure peculiar, individually, to both copper and titanium. One important technique in welding copper is that of "Direction of Travel." Cracks are found to prevail in the forehand method but will not appear in the backhand method where the filler metal is added to the weld puddle behind the arc resulting in a more gradual cooling rate. When added in front of the arc, it is felt that the parent metal expansion from heat causes larger stresses to be set up when the puddle solidifies thus causing the cracks to develop. Titanium, on the other hand, remains unaffected by either the forehand or backhand travel technique.

Ladies' Night Party

Detroit, Mich.—Friday night, May 14th and Saturday night, May 15th, at 6:00 P.M., have been set aside for the Ladies' Night Party by the *Detroit* Section. The party will be held at the Latin Quarter and will include dinner, door prizes for the ladies, favors, dancing and floor show. J. R. Stitt, Secretary-Treasurer of the Section, is in charge of the reservations.

Hard Surfacing

Detroit, Mich.—At its meeting of February 12th, the *Detroit* Section was extremely fortunate in having an outstanding speaker in the person of Howard S. Avery AWS, Research Metallurgist of the American Brake Shoe Co., Mahwah, N. J., in presenting an extremely difficult subject, namely, "Hard Surfacing to Minimize Industrial Wear." The subject was extremely well presented with proficient use of illustrative slides.

The dinner meeting was held at the Engineering Society of Detroit. Coffee speaker was Lawrence Loffman, State Agent for the Royal Liverpool Group, who touched on the subject "Your Insurance Troubles."

The evening was a very worthy one.

Powder Cutting

Erie, Pa.—A semitechnical talk on the subject "Powder Cutting" was given by T. G. Kells AWS, Service Engineer for the National Cylinder Gas Co., at the March 17th dinner meeting of the *Northwestern Pennsylvania* Section, held at the Summit Hotel in Erie.

The increased use of stainless steel and nonferrous alloys has presented a problem to the metal working industry. "Ferojet," a powder cutting process, developed by the National Cylinder Gas Co., provides a method of torch cutting these alloys. This process was described by Mr. Kells and the equipment used was displayed.

Through the courtesy of the Anaconda Copper Mining Co., a sound color film entitled "The Story of Chile Copper," was shown after the meeting.

Panel Quiz

Grand Rapids, Mich.—The First Annual Quiz Night held by the *Western*

Michigan Section on March 22nd was such an excellent meeting that the members voted to hold another one next year.

The Panel members who took part in this first quiz were: Stuart Spice, Buick Motors; Jim Williams, Buick Motors; Keith Sheren, Taylor-Thompson Machinery Co.; N. A. Reinke, Ionia Manufacturing Co.; Mr. Griffin, United Steel & Wire, and Dick Brennan, Hayes Mfg. Corp. Moderator was John Borman, Clark Equipment Co.

Plant Visit

Houston, Tex.—A plant visit to the Wyatt Metal and Boiler Works was made by 39 members of the *Houston* Section on March 24th.

Sabine Division

Houston, Tex.—The third meeting of the *Sabine Division* of the *Houston* Section was held on March 25th in the Sky Room of the Edson Hotel in Beaumont, Tex., with an attendance of 87 at the dinner and meeting.

W. G. Theisinger AWS, of the Theger Co., Houston, presented an excellent semitechnical discussion on "The Manufacture, Fabrication and Use of Clad Plates." Slides were used to illustrate Mr. Theisinger's talk.

Two sound color films entitled "Magic Wand of Industry" and "Building for the Nation" were shown through the courtesy of the Lincoln Electric Co. and U. S. Steel.

Spot-Welding Titanium Alloys

Indianapolis, Ind.—An exceptionally interesting talk was given on the "Spot Welding of Titanium Alloys" (THE WELDING JOURNAL, **32**(11), 1057(1953)) by E. F. Holt AWS, Chief Engineer, Welding Division of P. R. Mallory and Co., Inc., at the February 26th meeting of the *Indiana* Section.

The program was preceded by dinner at Buckley's Restaurant and an entertaining travel picture was shown by E. O. Smith, Hobart Arc Welding Equipment Co., Indianapolis, Ind.

Charles Moulton was introduced as a new member.

Forty-five members and guests attended this meeting.

Alloy Electrodes

Long Beach, Calif.—Richard K. Lee AWS, Vice-President of Alloy Rods Co., gave an excellent extemporaneous talk on "Recent Developments in Alloy Electrodes" at the February 19th dinner meeting of the *Long Beach* Section, held at Millers Restaurant.

Resistance Welding

Los Angeles, Calif.—The March 4th meeting of the Aircraft and Rocketry Welding Panel of the *Los Angeles* Section was called "San Diego Night" in that the program was sponsored by the members of the San Diego aircraft industry. Tom Bradley, A&RWP Vice-Chairman, presided over the meeting as San Diego's representative.

The technical discusser for this evening was Dennis Samuelson AWS of the Solar Aircraft Co. in San Diego. Mr. Samuelson's talk entitled "Welding Multiple Thicknesses by Resistance Welding," centered about the production of P2V Nacelles by spot welding. The Nacelle is made from Type 302 stainless steel sheet, 0.030 to 0.060 gages. Many closing junctions of the transverse and longitudinal ribs and skins are "pile-ups." These "pile-ups" range from 3 to 7 various gages.

Before discussing the type of equipment and welding procedures, Mr. Samuelson talked at length about quality control and inspection requirements. Several charts were shown giving a bird's-eye view of the astronomical costs involved in making test specimens for machine certifications. The fabrication specifications covering resistance welding require both macro and tension shear tests. Mr. Samuelson felt that one of these requirements could be eliminated, specifically the tension shear test. Innumerable tests logged from production schedules have shown that the T/S ratio of spot welds are very closely related to corresponding spot-weld macros.

Many slides of 3 and 4 pile-up macros and T/S strength charts were shown. As these numerable macros and charts were discussed and explained it became apparent that continuous T/S checks were not needed. The T/S tests were only required to check the welder at each shift change.

The conclusions of Mr. Samuelson's lecture were: (1) As the weld nugget diameter increases the tension shear (T/S) value increases. (2) The electrode diameter equals $2t \pm 0.010$ in., but due to practicability the electrode general never falls below $^5/_{32}$ in. in diameter. (3) "Pile-up" spot welds give higher T/S values.

Quiz Panel

Louisville, Ky.—The regular monthly meeting of the *Louisville* Section was held on March 23rd at Korfage's Tavern.

Following the dinner, a quiz panel session took place, which was a most interesting discussion. The quiz panel was composed of the following four men: James J. Hennessey AWS, General Electric Co.; Joseph X. Merkt AWS, Air Reduction Sales Co.; Harrington C. Powers AWS, American Air Filter, and J. T. Saiter AWS, Linde Air Products Co.

Questions were written by members and guests on cards provided before the meeting began. Provision was made so that those thinking of additional questions during the quiz session could write them on the cards which were collected by an assistant and given to the moderator.

The Moderator was Everett S. Elwood AWS, of General Electric Co., who read all questions from the cards to the panel.

Time was alloted at the end of the meeting for verbal questions and discussion. This was a most interesting and informative meeting.

Testing Welded Products

Milwaukee, Wis.—For the February meeting, held on the 26th, the *Milwaukee* Section returned to its regular meeting place, the Rathskeller of the Ambassador Hotel. Cocktails were served prior to the dinner, which was exceptionally well attended.

After dinner, Fred Robers of the Sport Diver Co., which represents the U. S. Divers Co. in this area, spoke on the subject "Aqualung." He explained that the aqualungs are used primarily for sport purposes—fishing and exploring underwater. In his discussion, starting with the historical background of its development in France, he continued with a description of its use, cost, apparel, physical conditioning, mechanical parts, etc. After his talk, everyone wanted to take up diving with an aqualung as a hobby.

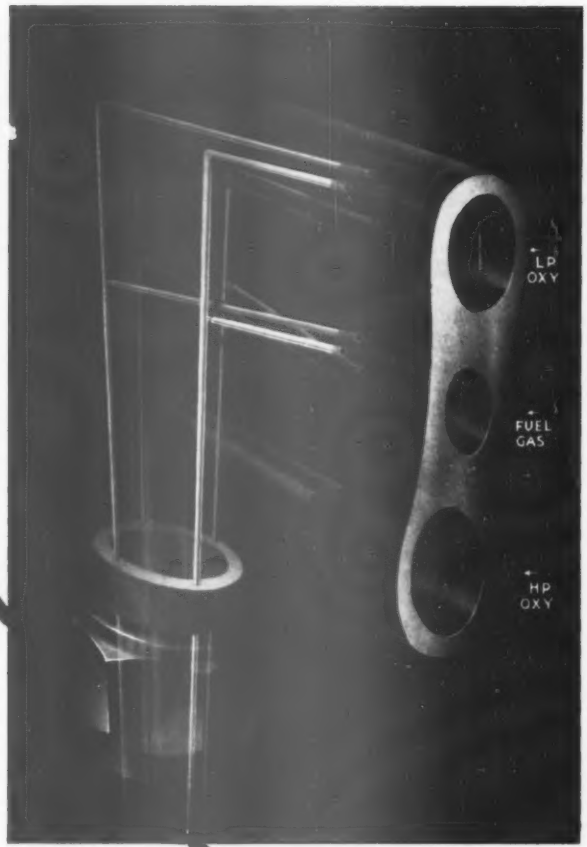
The technical speaker was William J. Poehlman AWS, Director of the Analytical Research Laboratory at the A. O. Smith Corp. and a member of the teaching staff of the Physics Department of Marquette University. The subject of his address was "Nondestructive and Analytical Testing of Welded Products" for which he is well qualified since he is considered an outstanding authority on the subject. His presentation was broad in scope covering the development of present day methods and instruments from the time when a hammer was used in the railroads to test for cracked rails and ending up by summarizing the latest methods of nondestructive testing and analysis and their interpretation. The problems encountered in bulk sampling, economical inspection, proper interpretation, instrumentation and man power were discussed relative to their importance in quality control inspection and production departments to assure management a profitable end product which is both competitive and has high consumer acceptance. The address covered the entire field of nondestructive testing by X-rays, spectrographic analysis, X-ray diffraction, etc., and was well illustrated with slides.

The meeting was adjourned by Chairman Werner Gallo after expressing the Section's sincere appreciation to the speaker.

Job Shop Welding

New York, N. Y.—"Job Shop Welding" was the subject of a panel discussion

this finer cutting torch



multiple mixer in torch head... it offers greater safety and greater efficiency...

A cutting torch mixer may be located either within the torch handle, the torch tubes or the torch head. When located within the handle or the tube, the mixer is situated in close proximity to the operator's hands; no torch, however, need be held by its head.

And, besides, there is plenty of metal in that torch head to permit for an added advantage: Multiple Mixing. By breaking up the streams of the low pressure oxygen as well as the fuel gas into several separate channels, the port diameters are lessened, thus offering greater flash-back resistance.

This modern, patented, Multiple Mixing in the torch head is costlier to manufacture but it eliminates the need for a separate mixer, leaky seats and joints, and years of experience in shop and field have proved the outstanding excellence of this patented design feature.

the mixer is located where it works best...and is safe.

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program presented to the *New York* Section on March 9th. C. A. Rose, Welding Foreman, A & P Metal Products, Inc., New York; J. Igoe, Welding Supervisor, The Hallen Co., Inc., Long Island City; and T. B. Jefferson, Editor, *The Welding Engineer*, Chicago, Ill., formed the panel of prominent job shop welding experts. Each panel member gave an introductory talk on one phase of job shop welding and then the meeting was devoted to questions from the audience. J. E. Mooney, of the New York Trade School, served as Technical Chairman and, as usual, was an outstanding chairman, keeping a lively discussion going, injected liberally with the usual "Mooney" humor.

An extra vote of thanks is due Mr. Jefferson in that he was a "pinch hitter" in the program as one of the speakers originally scheduled was unable to attend the meeting. Mr. Jefferson is a past-chairman of the Chicago Section AWS.

The meeting was held at Schwartz's Restaurant, 54 Broad St., New York City, where dinner was served prior to the technical session.

Activation Meeting

New York, N. Y.—The *Long Island Division* of the *New York* Section held an Activation Meeting on Mar. 2, 1954, at the Long Island Agricultural & Technical Institute, Farmingdale, L. I. Present were J. Alvar, F. G. Bell, C. W. Berry, C. W. Briggs, H. R. Gordon, T. F. Imholz, J. Jungbluth, N. Kopchinski, H. Leary, A. C. Lodestro, W. C. MacLaury, E. W. Moles, F. J. Mooney, E. B. Morris, J. Nocar, R. M. Platz, W. W. Rogers, A. A. Strickland, L. Van Kollem, D. Whitelaw and A. G. Wood.

The opening address was given by E. W. Moles, Acting Chairman, who outlined the purposes of the group to be formed as the Long Island Division of the New York Section. Mr. Moles advised that the New York Section had advanced $25 for initial expenses and would shortly send out cards to all members of the New York Section calling attention to the organization of the Long Island Division.

Earl B. Morris, Acting Chairman of the combined Program and Arrangements Committee, advised that arrangements have been completed for a dinner meeting to be held Tuesday evening, April 27th, at Wheatley Hills Tavern, in Westbury.

Messrs. Berry and Morris, who have been handling publicity for the Activation Committee, advised that announcements and articles would be published in house organs, magazines and newspapers.

Various acting committees were formed and are listed in their order of formation at the meeting:

MEMBERSHIP COMMITTEE
Chairman: J. Jungbluth assisted by L. Van Kollem.
PUBLICITY COMMITTEE
Chairman: L. Roth assisted by H. Leary.
BY-LAWS COMMITTEE
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ATTENDANCE COMMITTEE
Chairman: H. R. Gordon.

F. J. Mooney, Assistant Secretary-Assistant Treasurer of the AMERICAN WELDING SOCIETY, addressed the gathering and reviewed the purposes and scope of the SOCIETY, its history and the immediate future program on a national basis. He also detailed the various types of memberships in the SOCIETY.

George F. Chapline (second from left), Fairchild Engine and Airplane Corporation Vice-President, congratulates S. T. Walter, Chairman, New York Section, marking formation Long Island Division. Also shown are S. A. Greenberg, Technical Secretary and F. J. Mooney, Asst. Secretary, AWS

Stag

New York, N. Y.—The Annual May Stag dinner under the auspices of the *New York* Section will take place on May 28th.

Weldments

Olean, N. Y.—The March meeting of the *Olean-Bradford* Section was held on Monday evening, March 22nd, at The Castle, Olean.

Forty-five members and guests ate dinner at 7:30 P.M. after which the meeting was addressed by John Mikulak AWS, Assistant to Vice-President in Charge of Manufacturing, Worthington Corp., Harrison, N. J. His subject "Designing for Tooling and Fabrication of Weldments" was illustrated with 42 slides.

The consensus was that Mr. Mikulak's presentation was outstanding and of material benefit to his listeners. The Section feels deeply indebted to Mr. Mikulak for his time and the benefit of his broad experience.

Rectifier Welder

Pascagoula, Miss.—The regular monthly dinner meeting of the *Pascagoula* Section was held on March 3rd, at the Ingalls Shipyard Cafe. A "down-to-earth" technical discussion of the basic principles of the rectifier type welder, couched in language that is readily understood by welding engineers and operators not too familiar with complex electrical theory, was given by G. K. Willecke AWS of the Miller Electric Manufacturing Co., Appleton, Wis. Mr. Willecke is a co-designer of this welder and served as chairman of an industry technical subcommittee to study and make recommendations on the problems of high-frequency stabilized welders.

History of Welding

Peoria, Ill.—The *Central Illinois* Section held its March dinner meeting on the 17th at the New Sazarac. "The Importance of Welding Circuit Efficiency" and "History of Welding," based on the history and development of the welding industry covering equipment, materials and methods, were presented by Roy W. Poe AWS, Sales Manager, Tweco Products Co., Wichita, Kan. This proved to be a very interesting subject.

G. I. Swift, Assistant Factory Manager, Steel Fabrication, Caterpillar Tractor Co., Peoria, was the technical chairman and introduced the speaker.

History of Brazing

Pheonix, Ariz.—The *Arizona* Section held its March meeting at the Silver Spur restaurant on Wednesday, the 17th.

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The guest speaker of the evening was A. M. Thompson AWS, Sales Engineer, Fabriform Metal Products, Los Angeles, Calif., who spoke on the "History of Brazing." Mr. Thompson's talk was well prepared and enthusiastically received. It covered the origin of brazing, its development, methods of heating, types of materials, shop practices and special considerations often required in unusual designs.

A film entitled "Miracles in Metals" was also presented showing the various types of furnaces used for copper and silver brazing.

Accompanying Mr. Thompson was Mario Blick, General Manager of Fabriform, who supplemented Mr. Thompson's presentation by giving additional information on brazing and answering questions.

All present agreed that the meeting was most worth while and appreciated the fine quality of the evening's program.

Developments in Electrodes

Portland, Ore.—The fifth meeting of the *Portland* Section of the current season was held on February 24th at the Heathman Hotel. The meeting was presided over by Neeley Wood, Program Chairman.

A social hour preceded the dinner, which was followed by a short business session and announcements. Prior to introduction of the speaker, a film entitled "No Finer Electrodes Made" was shown through the courtesy of the Alloy Rods Co.

The speaker of the evening was R. K. (Dick) Lee AWS, Vice-President of Alloy Rods Co., York, Pa. A highly informative and interesting talk on "Recent Development in Alloy Electrodes" was enjoyed by 52 members and guests.

Nickel Alloys and Cast Iron

Saginaw, Mich.—Approximately 66 members and guests of the *Saginaw Valley* Section attended the dinner meeting held on March 11th at the High Life Inn in Saginaw, to hear K. M. Spicer AWS of the International Nickel Co. Right after dinner, a movie entitled "Greentree Thoroughbred" was shown depicting the life of a thoroughbred race horse from the day it was born to successful performance on a race track.

Mr. Spicer's talk covered the maintenance welding of nickel, high nickel alloys and cast iron, using the various welding processes which included metallic arc, inert arc and oxy-acetylene. Illustrations of the proper joint design and fit-up were shown together with improperly welded joints. Cleaning parts before welding was also stressed, especially from the sulfur and lead bearing materials which cause embrittlement of the weld whenever proper cleaning methods are not employed. Silver brazing, testing of welded specimens and the welding of cast iron and ductile iron was also covered.

Inert-Metal Arc Welding

St. Louis, Mo.—The February 12th dinner meeting of the *St. Louis* Section was held at the Forest Park Hotel. Cocktails were served prior to the dinner. Ninety members and guests heard an exceptional talk by Aksel Gander AWS, Process Representative, Air Reduction Sales Co., St. Louis, and saw an exceptional movie on the subject "Inert-Gas Metal-Arc Welding."

The color film was made at extremely high shutter speed and projected at such slow speed that the actual deposition of the electrode particles could be observed. The effects of variation of current settings, gases and polarity could be readily determined.

Mr. Gander explained that there are two methods of inert-gas metal-arc welding; namely, the tungsten arc process and the consumable type of electrode process. He also explained the history of the process, in that for some time there has been a demand for some process of welding nonferrous materials, particularly aluminum, other than the ordinary processes. He said that Airco did considerable research to fill this demand during the war and that the U. S. Navy was the first to use consumable type of electrode for inert-gas metal-arc welding. This was used for aluminum, and he said that this process works especially well on aluminum.

He also pointed out that either a hand gun or an automatic welding head could be used for this purpose.

Mr. Gander said that according to their experiments, when welding zirconium and titanium, they have found that best results are obtained by using an auxiliary gas field to give more complete gas coverage.

He also pointed out that the extremely high current densities used in this process make very good fit-up a necessary requisite. He explained that with this process a smaller bevel can be used than for ordinary processes. Mr. Gander stated that on nonferrous material argon or helium are equally good. He said that at the present time, his company is experimenting with use of mixed gases. The speaker also stated that this process can be used on copper, austenitic stainless steels, magnesium, bronze, Monels, nickels and clad materials, in addition to the aluminum for which it was first designed. It can also be used to weld mild steel to meet the ASME Pressure Vessel Code.

He pointed out that in welding bronze, a stranded spiral type of wire is used. The speaker also pointed out that in many cases, the addition of oxygen to the inert gas is beneficial, because it has a wetting action which improves arc stability. He said that oxygen is added on the order of 2 to 5%.

Welding Electrodes

San Francisco, Calif.—The regular monthly dinner meeting of the *San Francisco* Section for February was held on the 23rd at El Curtola in Oakland. Following a "Goodfellowship" hour and dinner, an excellent coffee talk was given by T. B. Jefferson AWS, Editor of *The Welding Engineer*.

Technical speaker was R. K. Lee AWS, Vice-President of the Alloy Rods Co., York, Pa. Mr. Lee very capably discussed and illustrated the uses and misuses of alloy welding electrodes of stainless and low-alloy steels.

Titanium

San Francisco, Calif.—The regular monthly dinner meeting of the *San Francisco* Section for January was held on the 25th at the Engineers' Club with an attendance of 81 at dinner and 84 at the meeting. Following the usual "Goodfellowship" hour and dinner, the meeting was opened at 7:45 P.M. with an excellent illustrated discussion on "Titanium, Metal of the Future" by Ward W. Minkler, Sales Metallurgical Engineer, Titanium Metals Corporation of America. Mr. Minkler covered the processing, application, fabrication and welding of pure and alloyed titanium. Current fabrication and welding practices as well as major applications and reasons for choosing titanium over less expensive materials were covered as well as the limitations of titanium. Samples of welded titanium were available for inspection. Also shown was a film entitled "Modern Aircraft" which featured descriptions and flight performance of the latest British Military Aircraft.

Low-Temperature Alloys

Seattle, Wash.—The March 11th dinner meeting of the *Puget Sound* Section held at the Engineers' Club was addressed by J. Marden of the Eutectic Welding Alloys Corp. Mr. Marden's excellent talk on "Bonding with Low-Temperature Alloys" was presented extemporaneously. A sound color film was shown in conjunction with the talk.

Pressure Vessels

Sheffield, Ala.—The *Tri-Cities* Section held its February meeting on the 25th at the TVA Auditorium in Sheffield. Speaker was R. A. Davis AWS, District Engineer, Chicago Bridge and Iron Co., Birmingham. Mr. Davis discussed the subject "Fabrication of Pressure Vessels."

Hard Surfacing

Shelton, Conn.—The March dinner meeting of the *Bridgeport* Section was held

on the 18th at Rapps Restaurant in Shelton. Speaker was Howard S. Avery AWS, Research Metallurgist, American Brake Shoe Co., Mahwah, N. J. Mr. Avery spoke on "Hard Surfacing to Minimize Industrial Wear."

Brazing

South Bend, Ind.—The *Michiana* Section spent the last two meetings concentrating on the use of copper base brazing, welding and overlay operations, as well as brazing done with other materials. On February, the 18th, F. E. Garriott AWS, of the Ampco Metal Co., Milwaukee, Wis., spoke on "Jobs You Can Do with Bronze Electrodes and Filler Metal," while March the 18th brought L. D. Richardson AWS, Eutectic Welding Alloys Corp., Flushing, N. Y., who spoke on "Brazing."

Mr. Garriott mixed a little prestidigitation in with a technical discussion of the use of aluminum bronze and other copper alloy electrodes to bring in the uses of these alloys. He emphasied the importance of knowing the base metal with which you are working and the properties required in the joint or overlay in order to obtain the best possible job. It is further important to recognize the deposition characteristics of the different rods, for there is no universal rod or method that can be applied to every job and come out satisfactorily. Take preheating, for instance. Iron and nickel base alloys are heated to 200–300°, bronzes to 300–400°, brasses to 500–800° and copper to 800–1000°, while you must use the higher temperature for a joint between dissimilar metals. Current likewise varies in the same order that the preheat temperature does.

These alloys are quite versatile in that they can be deposited by metal arc, carbon arc, inert-gas tungsten arc, inert-gas consumable electrode and submerged arc methods as well as with oxy-acetylene.

Mr. Richardson confined his remarks to the brazing and soldering fields, application mostly by oxy-acetylene. He also stressed the fact that there is no one material that suits all conditions, the perfect, universal brazing material being strictly a figment of the imagination. The use of the proper flux was noted as being of utmost importance. By means of a few samples and some tin-lead solder, he demonstrated the difference between fluxes in aiding the brazing material to wet and adhere to the base metal.

Numerous slides were shown of jobs that can be done with brazing methods.

Pipe Welding

Springfield, Ill.—The *Sangamon Valley* Section held its March dinner meeting on the 9th at the Mill Tavern in Springfield. A semitechnical talk on "Pipe Welding in Petroleum Industry" was given by Albert W. Zeuthen AWS of the Socony Vacuum Oil Co.

Submerged Arc Welding

Syracuse, N. Y.—The *Syracuse* Section met on March 10th and approximately 60 members and guests were present. Technical speaker was Harry J. Bichsel AWS, Design Engineer for Westinghouse Electric Corp., who spoke on "Applications for Submerged Arc Welding." Mr. Bichsel

Speaker H. J. Bichsel

gave a brief history of automatic welding, outlining the development of processes and equipment leading to submerged arc welding. Bare wire and coated wire welding were covered briefly to contrast these older processes with the subject. Limitations of type of seams, joint design and fixture design were discussed. Selection of wire diameter, voltage, current and speed were considered. Effects of straight and reverse polarity dc were discussed with a comparison to a-c power supplies. Some typical joint designs were shown. Several different methods of preparing plate edges were discussed as well as backing methods and standard work-handling tools.

Certification of Welders

Tucson, Ariz.—The *Tucson* Section held their regular monthly meeting on March 16th at the University of Arizona's Mechanical Engineering Department.

Prof. William C. Currie, the Section's Technical Secretary, presented a very interesting program. With the assistance of three of his students, Prof. Currie showed the members the procedures used in certifying of welders and the equipment used to test weld deposits.

Weld samples were tested for tensile strength, yield point and elongation as well as bend bar tests. Following this very interesting talk and demonstration, Prof. Currie showed the members the technical library of the Section which he maintains in his office.

Welding Electrodes

Tucson, Ariz.—The February meeting of the *Tucson* Section was held on the 16th at the Tucson Municipal Golf Course Club House. An excellent talk was given by R. K. Lee AWS, of the Alloy Rods Co., on "Recent Developments in Alloy Welding Electrodes." This is one of the most interesting talks the Section has had the privilege to hear. Mr. Lee's talk was well illustrated with slides and sound films.

Bronze Electrodes

Tucson, Ariz.—The *Tucson* Section held its regular monthly meeting on January 19th at the Tucson Municipal Golf Course Club House. F. E. Garriott AWS, of the Ampco Metal, Inc., gave a presentation of general shop interest and described the general usage of bronze electrodes and filler rods and the selection of the proper one for specific applications. His talk covered the usage of these electrodes for the joining of iron-base metal, dissimilar metals, overlay welding for bearings, wear and corrosion-resisting applications.

Mr. Garriott's talk was very interesting and well illustrated with slides.

Welding Movie

Worcester, Mass.—The March dinner meeting of the *Worcester* Section was held on the first at the Tower House. A non-technical talk was given by A. Andre Gelinas, Assistant District Attorney. Mr. Gelinas told of some interesting incidents which came up in court.

Through the courtesy of the Worcester Chamber of Commerce, a sound color film entitled "Distortion" and "Welding Comes to the Farm" was shown before the meeting.

Fillerarc Welding

Youngstown, Ohio.—The *Mahoning Valley* Section held its March dinner meeting on the 18th at El Rio Restaurant. An excellent semitechnical talk on the subject "Fillerarc Welding" was given by R. W. Tuthill AWS, Welding Engineer for the General Electric Co.

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INDEX TO ADVERTISERS

Acorn Iron & Supply Co. 485
Air Reduction . . . . . Outside back cover
Alloy Rods 501
American Chain and Cable 505
Ampco Metal, Inc. 511
Anaconda 499
Aronson Machine Co. 487

Becker Brothers Carbon Co. 493

Crucible Steel Co. 521

The Diversey Corporation 515
Drawalloy Corporation 496

Eutectic Welding Alloys Corporation 494

Fansteel Metallurgical Corporation
W. W. Alloys, Inc., Division 489

General Electric 506, 512, 513

Hobart Brothers Company 427

The International Nickel Company, Inc. 432

Jackson Products . . . . . Inside back cover

The Lincoln Electric Company 503
Linde Air Products Company, a Division of
Union Carbide and Carbon Corp. 430

P. R. Mallory & Co., Inc. 429
Metal & Thermit Corporation 428
Miller Electric Manufacturing Co. 508, 509

National Carbide Company 488
National Cylinder Gas Company 495
National Welding Equipment Co. 519

Page Steel and Wire Division 505

Smith Welding Equipment Corp. 489
Square D Company 507
Stahl Equipment Company 498
Stulz-Sickles Co. 500

Tempil° Corporation 524
Tube Turns, Inc. 491, 492
Tweco Products Company . . . . . Inside front cover

Union Carbide and Carbon Corporation
Linde Air Products Company 430
United States Testing Company, Inc. 498

W. W. Alloys, Inc.
Division of Fansteel Metallurgical Corp. 489
Welding Fittings Corp. 497
Westinghouse Electric Corporation 493

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Supplement to the Welding Journal, May 1954

Directionality of Mechanical Properties in Hot-Worked Steels

Rolled or forged steel does not have the same mechanical properties in all directions. PVRC-sponsored report discusses extent to which this directionality, as found in hot-worked steels, is influenced by certain variables

by A. R. Troiano and L. J. Klingler

Summary

Steel that has been rolled or forged does not have the same mechanical properties in all directions. This directionality is called anisotropy and occurs in both cold-worked and hot-worked steel. In pressure vessel steels the greatest interest is in the properties parallel to and transverse to the rolling or forging direction.

In rolled, forged or otherwise hot-worked steel, the tensile and yield strength exhibit little or no directional properties over a wide range of testing temperatures including subzero temperatures. However, other related tensile properties usually show anisotropy. Both elongation and reduction of area, for instance, decrease markedly as the angle between the test specimen axis and the rolling direction increases and approaches 90 degrees.

All notch properties exhibit directionality and invariably are inferior in the transverse direction, although the magnitude of the difference will, in some cases, depend upon the test employed and the criteria selected for comparison. This applies to the transition temperature, which may be considerably higher for specimens in which the notch is parallel to the direction of rolling than for specimens notched transverse or perpendicular to the direction of rolling. There is some indication that fatigue properties also are affected.

The extent to which hot-worked steels exhibit anisotropy depends upon many variables. One of these is the amount of reduction in working. It has been demonstrated repeatedly that reduction by forging up to 3:1 or 4:1 improves both longitudinal and transverse properties (tensile and yield strength excepted). With continued reduction beyond this ratio the longitudinal properties improve only slightly but the transverse properties deteriorate markedly. Cross rolling tends to reduce the amount of directionality in plates.

Steel quality, as evidenced by nonmetallic inclusions, banding, segregates, porosity and voids, is probably the single most important variable which influences the amount of directionality found in hot-worked material. Evidence indicates that the number of discontinuous stringers of oxides and other brittle inclusions fragmented in rolling, as well as continuous stringers such as silicates and sulfides, correlates quite well with transverse ductility. As the inclusion count increases, transverse ductility decreases while the longitudinal ductility is not affected appreciably. There is agreement that nonmetallic inclusions account for only a part of the anisotropy, and that segregation, such as carbide stringers, as well as heterogeneous distribution of elements in solid solution accounts for most of the remainder.

Heat treatment *per se* appears to have little effect in reducing anisotropy. Homogenizing treatments have, in some instances, improved transverse properties by eliminating banding and segregation but, as the nonmetallics are unaffected, such treatments cannot be relied upon to reduce directionality to any important degree. However, the amount of anisotropy may vary appreciably with changes in strength level produced by heat treatment and, in general, anisotropy increases as the strength level of the steel is increased by heat treatment.

Some forms of heat treatment may embrittle steel even at low-strength levels. An example of this is temper embrittlement, a phenomenon which occurs more or less in all steels if treated at appropriate subcritical temperatures for sufficient time, such as may be encountered in postweld stress-relief treatments. There is a greater susceptibility to temper embrittlement in the transverse than in the longitudinal direction.

Rolled plate used in construction of pressure vessels usually will exhibit lower ductility in the transverse direction than in the longitudinal direction. This difference, which is a function largely of steelmaking and rolling practice, tends to be relatively

A. R. Troiano is Head, Department of Metallurgical Engineering, Case Institute of Technology, Cleveland, Ohio. **L. J. Klingler**, formerly Senior Research Associate, Case Institute of Technology, is now Research Engineer, Engineering Research Laboratory, E. I. du Pont de Nemours & Co., Wilmington, Delaware.

low in cross-rolled plate. Since this directionality in properties is not recognized by any of the applicable Codes except in limiting the maximum thickness of structural quality plate, and since no failures of pressure vessels have been attributed to it, it is not always rated as a significant factor in pressure vessel design or fabrication. However, in view of the fact that directionality in mechanical properties is known to exist in hot-rolled steel plate, additional work seems warranted to determine the magnitude of this effect in typical commercial plate of different thicknesses and under various conditions of manufacture and treatment.

Materials Division
Pressure Vessel Research Committee, WRC

INTRODUCTION

IT HAS long been recognized that rolled or forged steel will exhibit different mechanical properties in different directions. This directionality or anisotropy will develop in both cold-worked and hot-worked steel. In cold-rolled material, it will originate from preferred orientation resulting from the alignment of specific crystal directions of each of the metal crystals as well as from the alignment of inclusions and segregates. Hot working does not normally produce crystallographic preferred orientation but may introduce marked directional properties mainly as the result of the alignment of inclusions and segregates. Since this discussion is not concerned with crystallographic anisotropy, and it is not possible to separate the relative contributions to anisotropy of crystal orientation from those of segregate and inclusion alignment, only the results from hot worked steel will be considered. The properties of greatest interest are those parallel to the rolling or forging direction, those transverse or perpendicular to this direction, and those in the thickness direction. No reliable data are available for properties of rolled plate in the thickness direction, thus only those in the longitudinal and transverse directions will be considered in this interpretive report.

BEHAVIOR

Tensile Properties

In rolled, forged or otherwise hot-worked steel, the tensile and yield strength exhibit little or no directionality over a wide range of testing temperatures including subzero temperatures. However, most other tensile properties show anisotropy. Figure 1, from the early work of Brearley[1] on rolled low-carbon steel plate, demonstrates that both the reduction of area and elongation decrease as the angle between the test specimen axis and the rolling direction increases and approaches 90 deg. Since this early study, much has been done with steels of various analyses subjected to many types of working but qualitatively the results have been the same. More recently other tensile properties have been investigated, such as fracture strength,* and the general shape of the true stress-true strain curve as might be evidenced by the strain hardening exponent n.† In rolled low-carbon steel

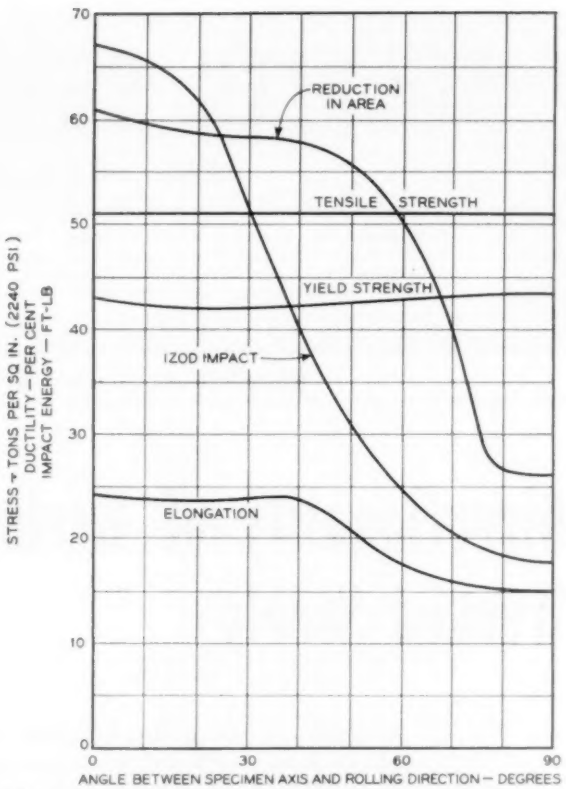


Fig. 1 Anisotropy in hot-rolled low-carbon Bessemer steel plate (Brearley[1])

plate, MacGregor[2] demonstrated that the fracture strength was appreciably lower in the transverse direction than in the longitudinal direction. Figure 2 shows the variation of fracture strength as the angle between

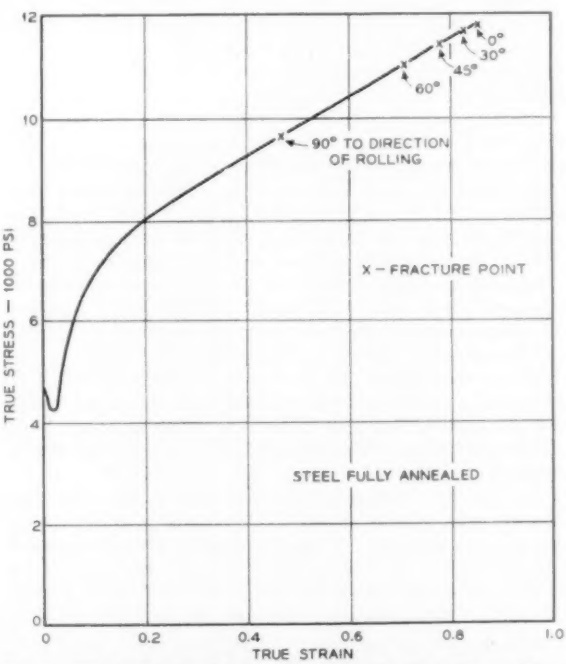


Fig. 2 The influence of anisotropy on the True Stress-True Strain Curve (Mehringer and MacGregor[2])

* Fracture load divided by fracture area.
† The slope of the true stress-true strain curve on a log-log plot.

the test specimen and the rolling direction increases. Apparently the shape of the true stress strain curve is not affected and the result of anisotropy is merely to cause failure to occur at a lower strength value. This behavior is in accord with other data[3] where it was determined that in ship steel plate the strain hardening exponent n was essentially the same in the longitudinal and transverse directions. Likewise, in compression tests the shape of the stress-strain curve was independent of the test direction.[4]

Notch Properties

All notch properties exhibit directionality and invariably are inferior in the transverse direction. This has been shown repeatedly in various types of notch tests, of which the notch impact test has received the greatest attention. From the early investigation of Brearley on rolled Bessemer plate (see Fig. 1) to the recent comprehensive and statistical studies of Wells and Mehl[5] on heat-treated basic electric steel forgings, it is apparent that impact specimens, taken 90 deg to the direction of working, consistently display lower impact energy than those taken parallel to this direction. In addition, the major change occurs as the angle between the specimen and the direction of working increases from approximately 20 to 70 deg. Figure 3, from the recent work of Bagsar,[6] demonstrates the effect of directionality on five different types of notch tests, two of which are notch-bend type tests. This

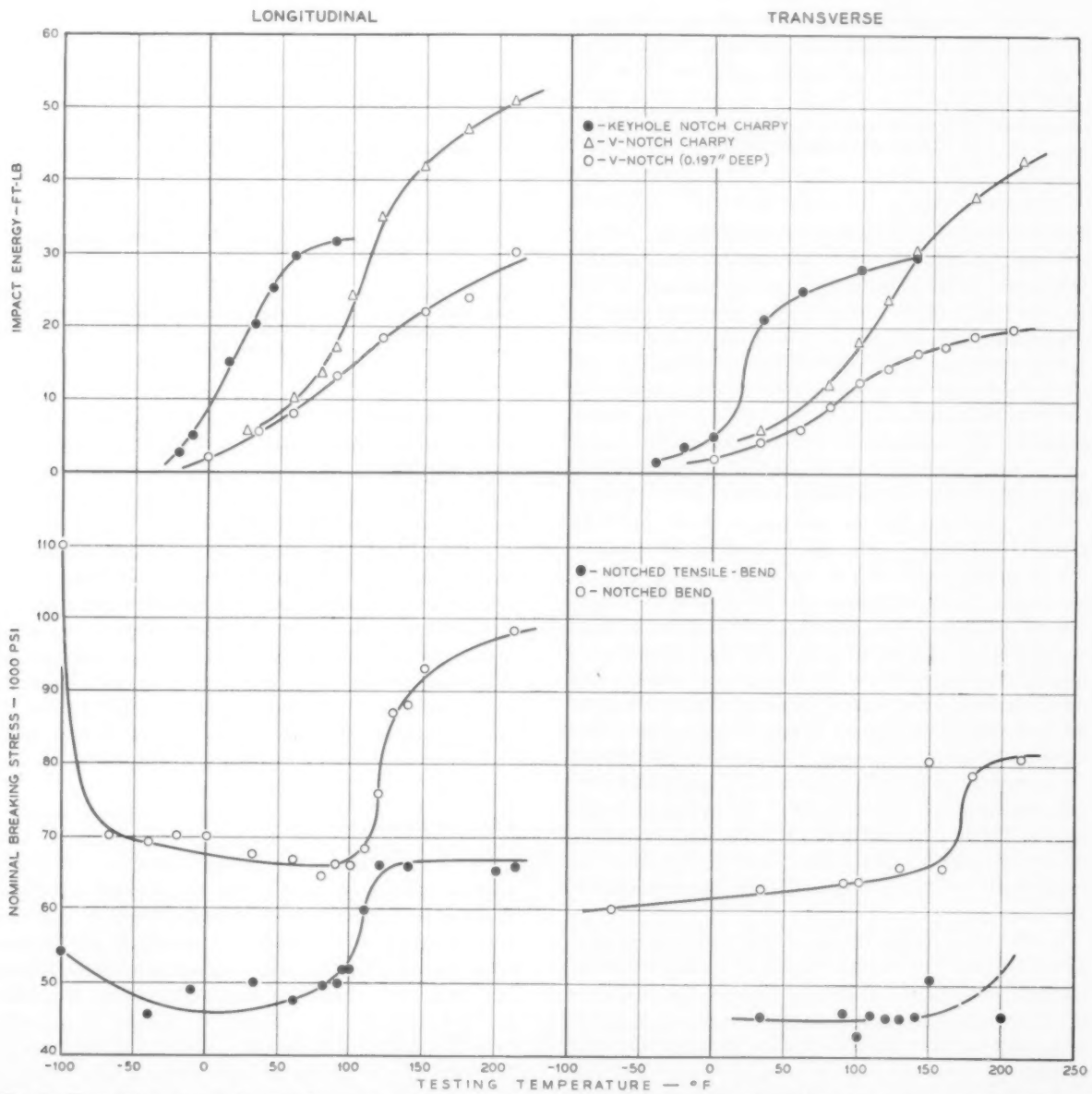

LONGITUDINAL
TRANSVERSE
IMPACT ENERGY - FT-LB
● - KEYHOLE NOTCH CHARPY
△ - V-NOTCH CHARPY
○ - V-NOTCH (0.197" DEEP)
NOMINAL BREAKING STRESS — 1000 PSI
● - NOTCHED TENSILE - BEND
○ - NOTCHED BEND
TESTING TEMPERATURE — °F

Fig. 3 Transition curves for specimens cut in the longitudinal and transverse directions for various types of noted specimens (Bagsar[6])

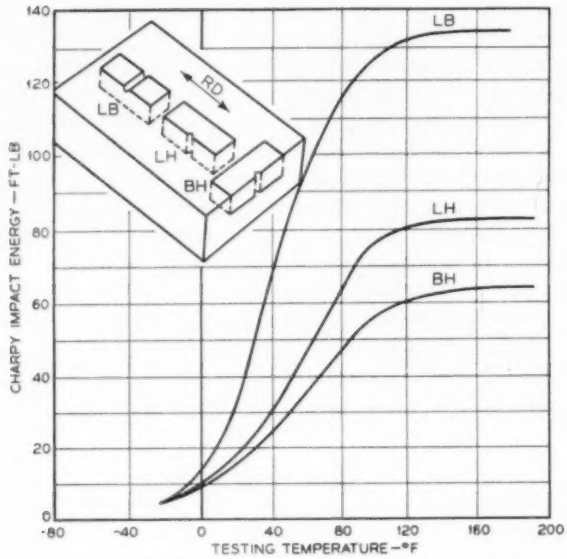


Fig. 4 V-notch Charpy curves for specimens cut at various orientations relative to the direction of rolling (Puzak, Eschbacher and Pellini[5])

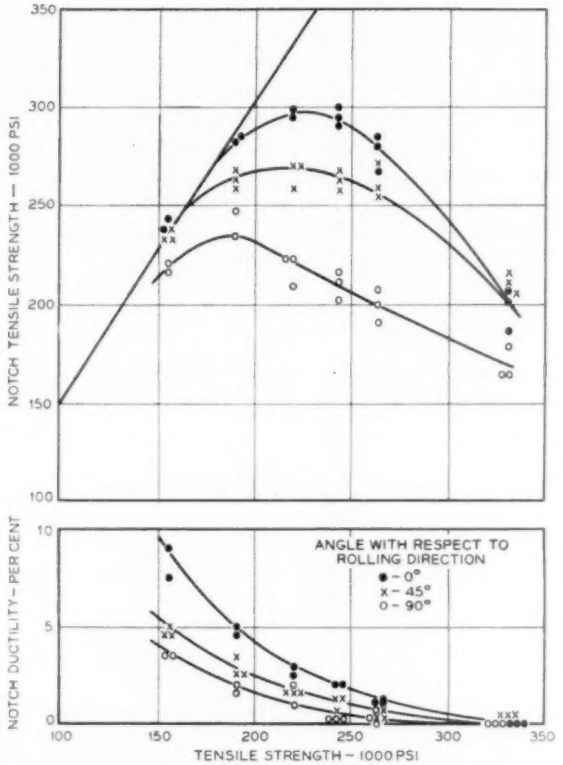

NOTCH TENSILE STRENGTH — 1000 PSI
NOTCH DUCTILITY — PER CENT
TENSILE STRENGTH — 1000 PSI
ANGLE WITH RESPECT TO ROLLING DIRECTION
● — 0°
x — 45°
o — 90°

Fig. 5 The notch tensile strength and notch ductility of SAE 4340 steel as a function of tensile strength and direction of specimen with respect to the rolling direction (Benkoe and Ebert[9])

work, performed on a 0.25% carbon, semikilled, $^{15}/_{16}$-in. hot-rolled plate of pressure vessel and ship plate quality demonstrated that the transition temperature in the direction transverse to the rolling direction may be appreciably higher than that found longitudinal to the rolling direction. It is interesting to note that, in the notch-bend test, there was a difference of as much as 50° F in the (ductility) transition temperatures determined in the longitudinal and transverse directions. In another investigation of ship plate steel, $^{3}/_{4}$-in. plates of 0.20% carbon-rimmed steel, with a stress-raising transverse slot at mid-length, were pulled in tension.[7] In these tests, the transition temperature was approximately 10° F higher for plates loaded transverse to the direction of rolling than for plates loaded parallel to this direction. Puzak, Eschbacher and Pellini[8] conducted an analysis of the initiation and propagation of brittle fracture in ship plate. The results shown in Fig. 4 are typical of those obtained for 30 steels including rimmed, semikilled and fully killed heats of ship-plate-type steel. The directionality effect on the energy absorbed was greatest at the higher testing temperatures and virtually disappeared at low impact levels. In addition, the 15 ft-lb transition temperature determined in the transverse direction was consistently 10–15° F higher than in the longitudinal direction.

Notch-tensile tests of hot-rolled and heat-treated SAE 4340 steel* by Benkoe and Ebert[9] conducted under carefully controlled conditions indicated that the fracture strength, reduction of area, notch-tensile strength and notch ductility are all lower for specimens oriented in the transverse direction than for specimens taken parallel to the rolling direction. The results of these tests, Fig. 5, show the variation of the properties with angle of test specimens and strength level. The tensile and yield strength (not shown) did not exhibit directionality at any strength level. The notch tensile strength, however, was substantially lower in the transverse than in the longitudinal direction at all strength levels and, in addition, the maximum value of the notch-tensile strength was displaced from a tensile strength of 225,000 to 190,000 psi. The transverse notch ductility was substantially lower than the longitudinal notch ductility at all strength levels. Other studies of a similar nature have yielded the same results.[10]

Fatigue Properties

The fatigue properties in the transverse direction may be inferior to those in the longitudinal direction. Ransom and Mehl[11] have recently made a comprehensive study of the statistical nature of the fatigue properties of SAE 4340 steel forgings reduced 20:1 from the ingot, and quenched and tempered to a tensile strength 125,000 psi. Figure 6 reports the results from one forging of high quality and one of low quality. The criterion of quality was the value of the reduction of area in the transverse tensile test. It is apparent that the endurance limit in the longitudinal direction

* The higher carbon steels, such as SAE 4340 (0.40% carbon, Ni-Cr-Mo steel) and 1045 (0.45% carbon steel), are not used for most pressure vessels; but are employed occasionally with special test and inspection requirements in certain integrally forged pressure vessels.

was approximately 50% of the tensile strength for both forgings but was appreciably less in the transverse direction, where the forging of low quality displayed an endurance limit of less than 37% of the tensile strength. In addition, a statistical analysis of a large number of tests on the same forging indicated that the inherent scatter in the fatigue strength was greater in the transverse than in the longitudinal direction; an effect which was more pronounced for forgings of relatively low quality and for strength levels near the endurance limit which means that individual values of transverse fatigue strength might be substantially lower than shown in Fig. 6.

Schmidt[12] observed that the longitudinal endurance limit was not more than 15% greater than the transverse value in a heat-treated alloy steel forging. Others have observed virtually no difference in endurance limit between the longitudinal and transverse directions.

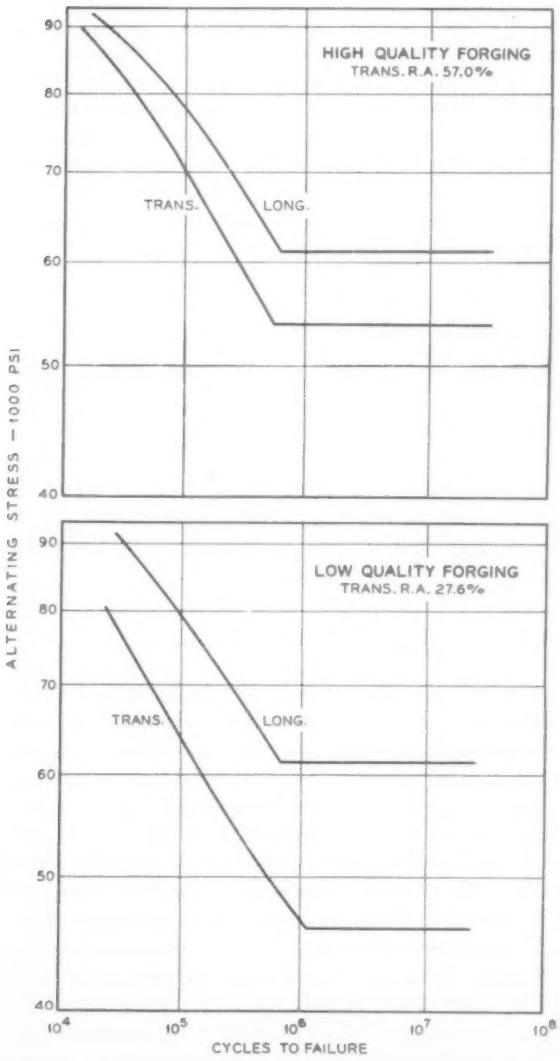


Fig. 6 Anisotropy exhibited by fatigue strength and endurance limit in two SAE 4340 forgings (Ransom and Mehl[11])

Fragmentary tests on hand-puddled wrought iron[13,14] and low-carbon sulfurized steel[15] revealed that the ratio of endurance limit to tensile strength was approximately the same in both the longitudinal and transverse directions, although the transverse notch impact values were less than half as great as those in the longitudinal direction. These tests were at strength levels of less than one-third the tensile strength of those which have demonstrated anisotropy of the endurance limit. However, there are insufficient reliable data to assess the significance, if any, of this difference in strength level.

Reduction by Forging

The extent to which hot-worked steel will exhibit anistropy depends upon many variables. One of the more important of these is the type of working such as rolling, forging, piercing, as well as the amount of reduction in working. There has not been any systematic study of the directional properties of a given steel after it has been worked in a number of different ways. On the other hand, the effect of the amount of reduction by forging on directionality has received much attention. The early work has been summarized by Sachs.[16] It has been demonstrated repeatedly that reductions by forging up to 3:1 or 4:1 improve both the longitudinal and transverse properties (tensile and yield strength excepted). Continued reduction beyond this improves the longitudinal properties relatively little; but the transverse properties deteriorate markedly for reductions at least up to 12:1. The early work of Kreitz,[17] summarized in Fig. 7, illustrates this for annealed 0.35% carbon steel. This has been confirmed in a recent comprehensive examination of an SAE 1045 forging steel by Grobe, Wells and Mehl[18] as well as by others.[12,16,19]

Cross Rolling

Cross rolling tends to reduce the amount of directionality. Herres and Lorig[20] found that the difference between the Charpy impact values of longitudinal and transverse specimens was reduced by cross rolling; and in one case there was no difference between the two sets of specimens in heat treated 0.25–0.40% carbon medium alloy steels. Presumably, in cross-rolled material the maximum anisotropy might be anticipated in some test direction near 45 deg. No study of the effect of test angle in cross hot-rolled material is available.

Steel Quality

Steel quality, as evidenced by nonmetallic inclusions, segregation, banding, porosity and voids, is probably the single most important variable which influences the amount of directionality found in hot-worked material. It is generally believed that these are the factors which give rise to mechanical anisotropy when

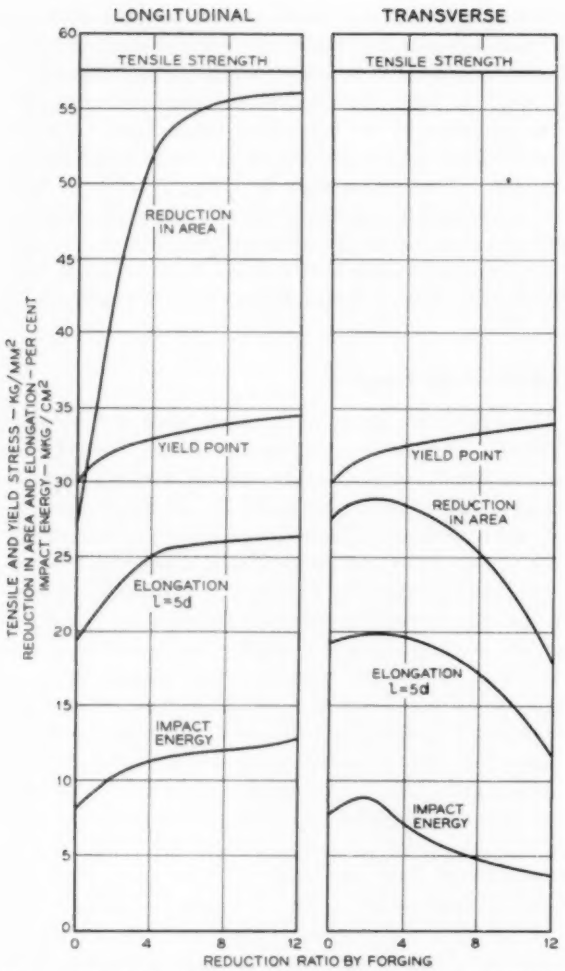


Fig. 7 The influence of forging reduction on anisotropy for a 0.35% carbon steel (Kreitz[17])

aligned in a specific direction as the result of hot working. All available information indicates that steel of high quality exhibits less directionality than steel of low quality. In recent years, the problem of inclusions and their relationship to steel quality, and thus indirectly to their effect on directionality, has received increased attention. The series of papers by Mehl, *et al*,[5,11,18] referred to above, carefully considered the effects of steel quality, measured in terms of transverse reduction of area, upon directional properties. It was concluded that elongated inclusions and segregation are largely responsible for the fact that the transverse ductility, notch and fatigue properties are lower in the transverse direction and demonstrate greater scatter than the corresponding properties in the longitudinal direction. This concept is consistent with the fact that these properties when measured in the transverse direction deteriorate quite sharply at forging ratios above 3:1 or 4:1; reductions above which the alignment of inhomogeneities and inclusions becomes pronounced. Welchner and Hildorf[21] examined the inclusions in 1100 specimens of the same type of steel as was studied by Mehl and co-workers. The number of discontinuous stringers of oxides and other brittle inclusions fragmented in rolling, as well as continuous stringers such as silicates and sulfides, correlated quite well with the transverse ductility. As the inclusion count increased, the transverse ductility decreased and exhibited greater scatter. Loria[19] concluded that sulfides were effective in promoting directionality only when in the form of stringers. These inclusions have little effect on the longitudinal ductility. According to Kinzel and Crafts,[22] the length of the inclusions correlated better with directionality than the total number of inclusions. However, there is general agreement that inclusions cannot account for all of the anisotropy.

Segregation

Presumably, segregation such as carbide stringers[23] as well as heterogeneous distribution of elements in solid solution will account for some directionality also. However, there is no reason to believe that all direction sensitive properties will be influenced to the same extent by inclusions as by segregation and banding. For example, wrought iron and very low-carbon sulfurized steel exhibited strong anisotropy in notch-impact tests but no effect on the endurance limit. In both cases, segregation and banding were virtually nonexistent but stringers of inclusions were highly prevalent.

Steelmaking Practice

Although the variables of steelmaking practice that control steel quality have received intensive study, the relationships of these variables to anisotropy have not been systematically investigated. However, one may infer that steelmaking practice will exert a strong influence on directionality, at least to the extent that it affects steel quality and heterogeneity as discussed above.

Heat Treatment

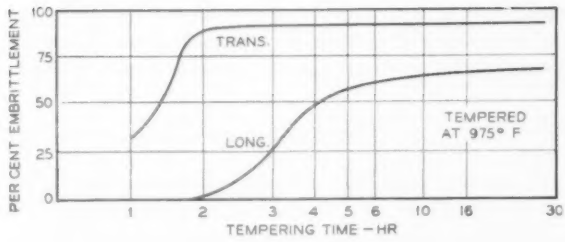
In many cases, heat treatment may be applied either deliberately to improve properties, or incidentally, as for example, all heating relative to a welding operation or incident to service at elevated temperatures. Heat treatment may be expected to exert only a minor effect on the distribution of nonmetallic inclusions; on the other hand, its influence on segregation may be appreciable. The limited information available indicates that homogenization can change both the longitudinal and transverse reduction of area. A homogenizing treatment designed to minimize banding in a high quality SAE 4340 forging improved both the longitudinal and transverse reduction of area by approximately 5% and reduced the scatter in the transverse values.[5] However, a similar treatment applied to an SAE 1045 forging resulted in a decrease of directionality;[18] which,

strangely enough, was accomplished by a decrease in the longitudinal reduction of area with little change in the transverse value. Any attempt to rationalize this behavior is limited by the fact that the relative contribution to directionality of elongated nonmetallics and of banding is not known quantitatively. In any event, homogenization is not a feasible commercial method of reducing directionality.

The amount of anisotropy may vary appreciably with changes in strength level produced by heat treatment. For pearlitic steels, as the tensile strength was raised the directionality was increased; a phenomenon which was accomplished largely by improvement in the longitudinal properties.[5,18,24] Apparently, this is true for pearlitic steels only; faster cooling rates with attendant increases in tensile strength will yield finer ferrite pearlite aggregates with improved reduction of area and notch-impact values. Martensitic steels both plain carbon and alloy, also exhibited increased anisotropy as the tensile strength was raised; but here it was brought about by a more rapid deterioration of properties in the transverse than in the longitudinal direction.[5,9,10,18]

Directionality exhibited by notch-tensile properties in steels with martensitic structure has been examined as a function of strength level.[9,10,23] Both the transverse and longitudinal notch ductility decreased as the tensile strength increased; but the longitudinal value decreased more rapidly approaching the transverse value near zero ductility (see Fig. 5). The behavior of the notch-tensile strength was somewhat more complicated. The transverse notch-tensile strength passed through a maximum at a lower tensile strength than the longitudinal notch-tensile strength; which, as Fig. 5 shows, will cause the apparent anisotropy of notch strength to pass through a maximum as the tensile strength is raised.

Heat treatment may severely embrittle steel even at low-strength levels. A classical example of this is temper embrittlement; a phenomenon which occurs more or less in all steels if treated at appropriate subcritical temperatures for sufficient time. Not only may temper embrittlement occur as a result of conventional heat treatment, but it also may result from postweld stress-relief treatments, or during service in a suitable temperature range, or in other ways incidental to processing and service. Jolivet and Chouteau[25] observed a marked effect of anisotropy in the susceptibility to temper embrittlement of a low-carbon alloy steel, i.e., impact energy values taken transverse to the rolling direction were affected appreciably more by temper embrittlement than those taken parallel to the rolling direction. The steel was tempered to a very low strength level so that subsequent treatments to produce temper embrittlement did not materially change the strength level. Figure 8 indicates conclusively that specimens cut in the transverse direction were more susceptible to temper embrittlement than those taken in the longitudinal direction. For example, tempering for 2 hr at 975° F resulted in virtually complete embrittlement in the transverse specimens and no embrittlement in the longitudinal specimens. Other tempering temperatures which resulted in temper embrittlement always embrittled the transverse specimens first. This means that, as a matter of practical engineering, tests conducted to evaluate the state of temper embrittlement should always be performed on specimens cut transverse to the rolling direction. This appears to be a matter of prime importance, especially in ship plate and pressure vessel type steels, in view of current thinking that plain carbon steels may be very susceptible to temper embrittlement[26] and that brittleness in the heat-affected zone of a weld may be associated with temper embrittlement.



$$\text{PER CENT EMBRITTLEMENT} = \frac{K_1 - K_2}{K_1}$$

WHERE K_1 = NOTCHED IMPACT ENERGY IN DUCTILE STATE

K_2 = NOTCHED IMPACT ENERGY IN BRITTLE STATE

Fig. 8 Effect of tempering time at 975° F on temper embrittlement of specimens cut in the longitudinal and transverse directions (Jolivet and Chouteau[25])

Strain and Aging

Little information is available dealing with the influence of strain and aging on anisotropy. The only study designed to evaluate these variables indicated that there was little if any effect. Tipper[27] examined $^1/_2$-in. mild steel plate that had been prestrained 3.75 and 10% in both the longitudinal and transverse directions and also subsequently aged. It was concluded that the direction of prestrain appeared to have little effect on the directionality of the impact energy as compared to the differences resulting from anisotropy produced by the original hot rolling.

Hydrogen

It has long been known that hydrogen will embrittle steel; but the influence of hydrogen on the properties of steel measured in various directions has received very little consideration. Hobson and Sykes[28] investigated the effect of hydrogen on the ductility in the longitudinal, transverse and radial directions of several low-carbon alloy steel forgings. In the absence of hydrogen, specimens in the transverse and radial directions were inherently less ductile than those in the longitudinal direction. The introduction of hydrogen produced a still further loss of ductility in the transverse and radial directions as well as reducing the ductility in the longitudinal direction. Although not

marked, there appeared to be a tendency for the radial and transverse directions to embrittle relatively more for the initial hydrogen additions up to 5 cc per 100 g.

DISCUSSION

In summarizing the influence of anisotropy, it becomes apparent that certain mechanical properties are influenced more than others, and some not at all. In addition, metallurgical variables such as heat treatment, as well as mechanical variables, will influence the degree of anisotropy observed in a given type of test. Consideration of the factors which influence the apparent ductility helps to rationalize what appears to be occasional inconsistent or inexplicable behavior. It is well known that, as the testing severity is increased, steel behaves in a less ductile manner. The decrease in ductility may be accomplished by superimposing multiaxial stresses, increased strain rate, lowered temperature or notches in the test specimen. The familiar impact transition curve (Figs. 3 and 4) indicates the manner in which the ductility is decreased as the testing temperature is lowered. The other embrittling agents listed above, or combinations of them will produce similiar behavior.

All available information indicates that when embrittlement occurs, properties in *the transverse direction are more susceptible to embrittling agents than those in the longitudinal direction.* It follows that an attempt to evaluate directional properties may become complicated by the occurrence of the transition effect regardless of its origin. For example, the test conditions may be such that the transverse specimens have become embrittled and the longitudinal direction ones have not. Under such circumstances, the difference between the observed test values in the transverse and longitudinal directions gives a distorted and incomplete picture of the anisotropy. Specifically, the following situations with regard to embrittlement are possible depending upon the mechanical and metallurgical variables involved:

(*a*) No embrittlement in either the transverse or longitudinal directions.
(*b*) The transverse specimens embrittled: the longitudinal specimens unembrittled.
(*c*) Both the transverse and longitudinal specimens embrittled.

From the above considerations, it is apparent that room temperature impact tests may be conducted under conditions of embrittlement corresponding to (*a*), (*b*) or (*c*), above. Obviously, only a consideration of the entire transition curve will properly assess the amount of anisotropy. A consideration of Fig. 4 illustrates this in convincing fashion. Here the orientation effects are apparent only above 10–12 ft-lb. In this connection, Puzak, Eschbacher and Pellini[8] reached, by correlation with other observations, the intriguing if provocative conclusion that, as far as engineering applications are concerned, brittle behavior is not observed unless the V-notch specimens exhibit less than 10 ft-lb impact energy; and hence that there is no practical effect of anisotropy with respect to brittle behavior in the steels they examined.

Under appropriate conditions, heat treatment will create the same embrittling action as low-temperature testing, or other previously mentioned factors. Thus, the behavior of temper embrittlement with respect to anisotropy may be rationalized on a transition phenomenon basis. Reference to Fig. 8 indicates that, after 2 hr at 975° F, the impact transition temperature for the transverse specimens was above room (test) temperature; while that for the longitudinal specimens was below room temperature. This situation corresponds precisely to the condition of testing listed under (*b*). Further examination of Fig. 8 shows that any one of conditions (*a*), (*b*) or (*c*) may prevail depending upon the tempering time. It is interesting to note that even when the maximum amount of temper embrittlement has apparently occurred, the transverse specimens are still embrittled appreciably more than the longitudinal specimens.

It is well known that, in general, prestrain will tend to induce embrittlement. Thus, in view of the greater sensitivity of the transverse direction to embrittlement it would be anticipated that prestrain would exert an influence on directionality. Although Tipper[27] observed little or no such effect, it is conceivable that under other conditions or with different materials such a phenomenon may manifest itself.

Those mechanical properties which exhibit anisotropy to some extent include: reduction of area, elongation, fracture strength, bend stress and strain (both notched and unnotched), notch-impact energy, notch ductility, notch tensile strength and, in some cases, fatigue strength and endurance limit. All of the above reflect the ductility in varying degrees, although there may be some question with respect to the fatigue properties, which seem to bear a more definite relationship to the tensile strength than any other property. However, there is some evidence of an effect of ductility, since those steels which exhibited lower transverse fatigue properties also exhibited lower transverse reduction of area values.[11] Also reductions of as much as 25% in the endurance limit have been observed in the embrittled state as compared to the unembrittled state for the same strength level and steel.[29]

The notch-tensile strength is influenced by ductility as well as by the tensile strength at the higher strength levels. In Fig. 5, it is apparent that the notch-tensile strength is directly proportional to the unnotched-tensile strength at low-strength levels up to the point where the curve departs from a straight line. At tensile strengths above this, the loss of ductility begins to influence the notch strength in increasing proportions as the strength level rises, thus introducing a maximum in the curve. That this maximum occurs at a lower strength level in the transverse than in the longitudinal specimens is consistent with the fact that specimens cut in the transverse direction are more sus-

ceptible to embrittlement. In addition, it is significant to note that anisotropy appears in the notch-tensile strength only when an element of ductility will influence the value of the notch strength.

It is concluded that although many different types of tests will exhibit anisotropy they do so only to the extent that they reflect ductility, and that ductility is the only basic property that exhibits anisotropy.

FIELDS FOR FURTHER STUDIES

It is evident that there are many gaps in the information on the effects of anisotropy. More information on the effect on directionality of temper embrittlement, hydrogen embrittlement and prestrain is needed. Also, there is little information on the effect of welding on directionality, particularly in the heat-affected zone. In addition, some attention should be given to a more direct correlation of steelmaking practice to anisotropy.

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Welding Metallurgy of Nodular Cast Iron

Discussion by T. E. Kihlgren

Mr. Hucke and Mr. Udin have presented an interesting and useful study of the structural changes which can occur in the heat-affected zone of the base metal and at the line of fusion in welded joints in Ductile Iron.

However, the authors arrive at some conclusions which would seem to rule out any fusion welding process for the salvage, repair and fabrication welding of ductile iron. Actually, field experiences show clearly that ductile iron can be fusion welded by several methods on a commercially practicable scale.

For example, Sohn[1] and his associates reported recently on the successful application, on a production scale, of arc welding of Ductile Ni Resist to Type 347 stainless steel, and of ductile iron to NAX steel, using AISI 431 and AISI 310 electrodes, respectively. The weldments are component parts of jet aircraft engines reported to have operated satisfactorily for long periods of time. This successful application of arc welding to ductile iron is in a field where quality standards are high and service conditions are severe.

A considerable amount of welding is being done in the field using a nickel-iron electrode commercially designated NiRod 55. Data on properties of joints, and examples of field welding were discussed in a recent paper by Waugh and Kihlgren.[2] Average tensile data[2] on joints in $^1/_2$- to 1-in. thick ductile iron made with the nickel-iron electrode are shown in Table 1. The base metal contained about 3.30 C, 2.30 Si, 0.70 Ni, 0.35 Mn, 0.05 Mg, < 0.05 P.

(*Continued on page 229-s*)

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Paper by Edward E. Hucke and Harry Udin was published in THE WELDING JOURNAL, **32** (8), Research Suppl., 378-s to 384-s (1953).

Working Conditions Ambient to Inert-Gas-Shielded Metal-Arc Welding

Exposure conditions in the breathing zone of inert-gas-shielded metal-arc welding with consumable electrode

by Leslie Silverman and Harry Gilbert

Abstract

The development of new welding techniques and procedures requires a reinvestigation of the possible exposure of operators to new or different health hazards. This particular study is primarily directed at conditions in the breathing zone of the newly developed inert-gas-shielded metal-arc welding process with consumable electrode.

This method of welding is quite simple and is probably familiar to most welding engineers. For purposes of elucidation and completeness, a brief description is presented here.

The principal feature of this unit is a helium or argon-shielded arc with controlled consumable electronic wire feed which may be manually directed or operated automatically by controls. Filler metals of various compositions are fed in wire form through the center of the shield which consists of a tube through which an inert gas (helium or argon) is passed. The wire is fed at a rate equal to the desired deposition for various practical applications. Varying currents and current densities are applied for different wire sizes but the densities encountered are many times those found with ordinary stick electrodes. For example, a wire diameter of $^{3}/_{32}$ in. when used with a recommended welding current of 300 amp gives a current density of 43,500 amp per square inch as compared to the current density obtained with a $^{1}/_{4}$ electrode of 6120 amp per square inch.

The higher current density produces a more confined source of spectral energy in both ultraviolet and infrared ranges. The excitation of various metals produces different atmospheric conditions and will be discussed at length. The choice of argon or helium depends principally on the metal welded. The gas flow rate is controlled by a pressure regulator and a flowmeter as in gas welding.

The purpose of this investigation was to study the atmospheric conditions in the welding zone with regard to the operator's exposure and to determine the concentrations and significance of the various gases and particulate matter produced. These were to be compared with various welding metals and with the conditions found in the conventional electrode welding. It should be understood that this study did not cover all available aluminum or stainless compositions and development of new materials might require further research. For example, since this study was completed, processes incorporating oxygen into the inert gas when welding on steel have been developed.

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The statements and opinions presented herein are those of the authors and do not necessarily represent those of the Navy Department.

AIR CONTAMINANTS PRODUCED BY WELDING OPERATIONS

CONTAMINANTS created by all-welding operations consist of gases as well as particulate matter. Welding gases from electrodes are principally oxides of nitrogen, ozone and traces of various other gases depending upon coatings or alloys used in electrodes. In stainless steel welding with coated electrodes fluoride compositions have been found by some investigators to yield traces of hydrogen fluoride gas. Similarly coatings containing chloride may yield hydrogen chloride. Certain aluminum alloys may produce trace gases such as phosphine or stibine. These are usually insignificant in concentrations unless a high percentage of active elements such as phosphorus are present (an unlikely condition). It is possible in welding exposure to have gaseous contaminants which vary widely depending upon the nature of the coating. In inert-gas-shielded metal-arc welding the coating is eliminated by the inert gas which provides the non-oxidizing atmosphere; thus, the only gaseous products (in addition to oxides of nitrogen and ozone, which are created from radiation and decomposition of the ambient atmosphere) are those resulting from trace elements in the welding metals.

Depending upon the metal excited it might be assumed that because of the higher current densities a greater concentration of nitrogen oxides and ozone could be produced. Because the primary source of energy is provided with an *inert gas shield*, most of the atmospheric breakdown must be accomplished by radiation although some atmospheric air could possibly be aspirated into the outer portion of the arc by the flowing stream or traces could reach the core by diffusion. Nitrogen oxide production usually requires intimate contact of the energy source with atmospheric nitrogen. The metal excited is important in atmospheric decomposition since the wave lengths that produce ozone are below 2000 A. In comparing spectral emission bands of iron and aluminum, the two important weld metals under consideration here, we find, as shown in Fig. 1, that iron lines are predominantly in

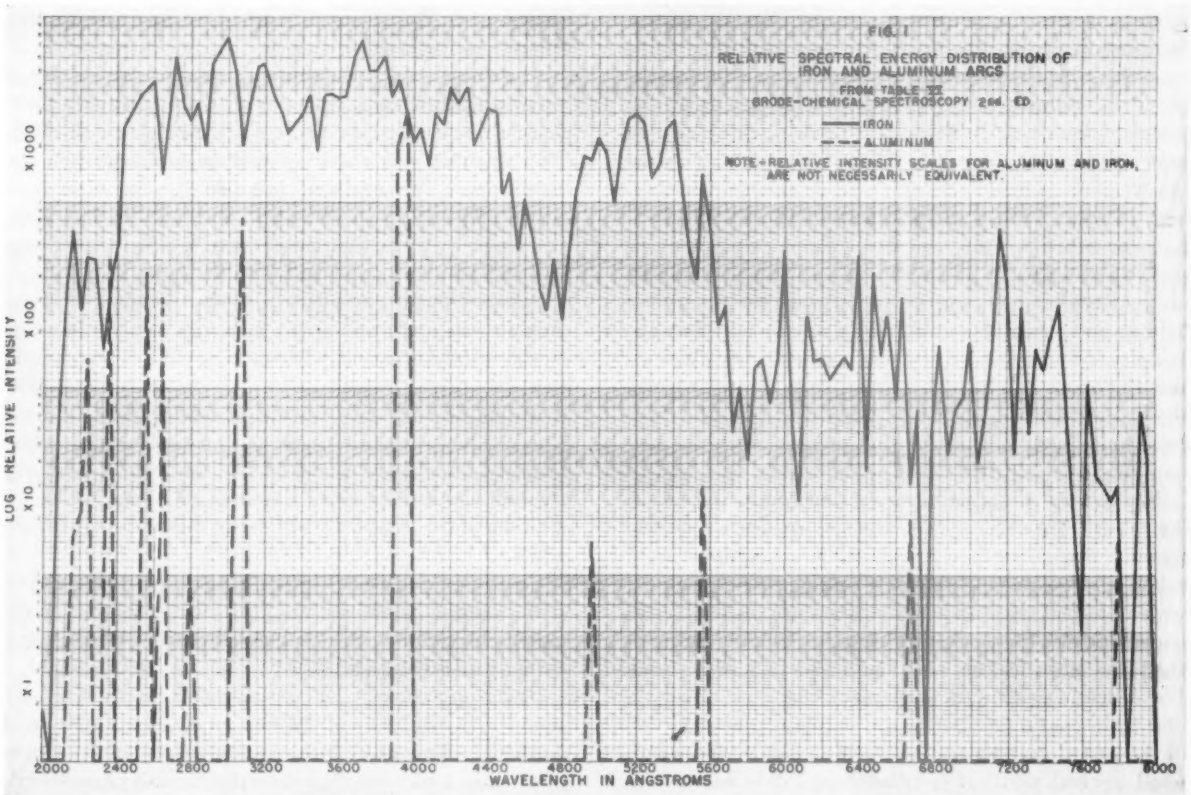

Fig. 1 Relative spectral energy distribution of iron and aluminum arcs

the blue and near visible, whereas radiation from aluminum is principally below 3900 A and the bulk of the radiation is concentrated in a few lines.

Particulates from all welding operations (*inert gas shield included*) depend, of course, upon the base and electrode metals and their composition and combustion products of coatings if any are used. *Inert-gas-shield welding* of the type described here has been largely applied in production to date on aluminum alloys and stainless steels. It could be used, however, with almost any alloy or combination desired. The process has been most effective for aluminum because of its ability to deposit aluminum at high deposition rates with excellent penetration in all positions. In addition to welding with nearly pure aluminum, the aluminum silicon alloys (up to 5%) have been used. For stainless steel work most of the common types such as 316 or 347 can be readily welded. The composition of the base metal on which the weld is deposited may vary, but is usually the same as the welding wire.

Particulate matter from arc or gas welding is classified as a fume; that is, a condensation product of the volatilized metal vapor. The particle size varies from 0.001 micron to above one micron, the larger sizes usually result from agglomeration of the fume by convection currents and atmospheric turbulence. Fume concentrations vary with the welded material, coatings, if any, and the current densities. In general, in inert-gas-shield work it would be surmised that the fume composition contains oxides of the metals being welded since metal vapor is evolved which has an opportunity to oxidize after leaving the inert shield, although in the shield zone it may not be actually oxidized. It may also consist of a metallic particle of metal coated with oxide. Since the fume originates from a vapor state and is of such a fine size it is probable that discrete particles oxidize rapidly on contact with air.

EXPERIMENTAL METHODS

Two types of experiments were conducted in this study. The first phase was a laboratory study on aluminum alloy and stainless steel welding under well-controlled conditions and the second study involved measurements obtained only on aluminum welding with and without exhaust ventilation in a field study made at the New York Naval Shipyard under actual working conditions. Since these measurements were made by two investigators in different locations a variety of sampling instruments and techniques were employed as outlined below.

Sampling Procedures

In the laboratory study fume samples were collected at 50 to 60 cfm by means of a high volume air sampler utilizing a pleated filter paper[1] and a hand pump sampling unit[2] described by one of the authors. Gas samples of oxides of nitrogen were obtained in 300 ml sample

bottles by the method suggested by Beatty, *et al.*[3] Samples of nitrogen oxides were collected during welding operations in the operator's working zone at breathing level. Ozone was also collected by means of the sampler described by Crabtree and Kemp.[4] These were taken in the operator's working zone and inside the welder's helmet. Samples inside the welder's helmet, of course, most nearly represent those concentrations breathed by the operator. Since ozone concentrations are greatly affected by the distance from the arc and the time elapse before entering the operator's helmet and various contaminants it was felt that the samples for ozone should be taken outside as well as inside the operator's helmet in order to evaluate properly the exposure of the operator. In a previous study[5] we have found a significant dilution present inside the helmet due to the air exchanged by the wearer.

In the field sampling investigation conducted at the New York Naval Shipyard, nitrogen oxides, ozone and metallic fumes were collected. The air samples were taken at the arc and in the working zone. Samples were obtained with and without the use of flexible exhaust ducts. For collecting fumes a commercial electrostatic precipitator sampler* (3 cfm) was used. For collecting ozone the apparatus consisted of a bubbler train containing potassium permanganate solution in the first sintered glass gas washing bottle, and a buffered neutral solution of potassium iodide solution in the second washing bottle.[6] Gas samples for nitrogen oxides were collected by using evacuated separatory funnels or 100 ml syringes using the Griess-Ilosvay reacting solution[7] for nitrogen dioxide. The latter method only determines nitrites and not total oxides of nitrogen.

Analytical Methods

Total fume concentrations in the laboratory studies were obtained from the previously weighed pleated filter samples and enough fume was collected to permit fractionation and evaluation of composition. Hand-pump samples were taken on small filter paper disks for comparison with standards previously made for stainless steel welding. However, no such standards were available for comparing stains obtained from aluminum alloy welding.

Total fume values from the field studies were obtained by accurately weighing the electrostatic precipitator tubes. After weighing, the pleated filter samples were analyzed for aluminum, aluminum oxide and silicon dioxide. It was originally intended to perform analysis for silicon as Si but only small amounts of aluminum as Al were found in the samples. It is evident that the silicon would have also been oxidized under the same conditions and therefore only analyses for silicon as silicon dioxide were performed.

Gas samples were analyzed for total oxides of nitrogen with the exception of one sample analyzed for phosphine. The chemical methods of analysis were as follows:

Aluminum (Al). Aluminum was determined by a modified cupferron procedure.[8] The pleated filter sample was leached with dilute hydrochloric acid (6N). This washing was made to volume and analyzed for aluminum. The material retained by the filter was pulped, digested with nitric acid and fused with sodium carbonate. It was then filtered and washed with dilute HCl (6N) and also analyzed for aluminum.

Silicon Dioxide (SiO_2) and Alumina (Al_2O_3). Silicon dioxide was determined by ashing the sample completely after digestion with nitric acid. This sample was accurately weighed, treated with concentrated hydrofluoric acid and the silicon removed by fuming. The residue was again taken to constant weight. The total weight change corrected for loss from a similarly treated blank filter gives the total SiO_2 loss. The remainder of the sample by difference was aluminum oxide Al_2O_3.

Chromium and Iron. Samples taken during stainless steel welding were analyzed for water-soluble hexavalent chromium with *s*-diphenylcarbazide[9] and for iron by mercaptoacetic acid.[10]

Oxides of Nitrogen. Gas samples were analyzed by the U. S. Bureau of Mines phenoldisulfonic acid method.[3] This procedure requires oxidation of nitrites to nitrates which are then determined with phenoldisulfonic acid.

Field samples for nitrogen oxides were limited to nitrite determination with the Griess-Ilosvay reagent as indicated above under sampling. This reaction produces a rose color which may be directly compared to standards.

Phosphine. Phosphine was determined by absorbing the gas sample in silver nitrate solution and measuring the turbidity nephelometrically. This test is not specific for phosphine since it also reacts with arsine or stibine, but it was not anticipated that either of these interfering gases would be present. Phosphine might be present because traces of phosphorus are present in the wire or the base metal. Observations on other aluminum welding processes indicated that this was a detectable contaminant. Because of the high toxicity of this gas it was desirable to make a check for its presence in this instance.

Ozone. Laboratory ozone samples were analyzed as described by Crabtree and Kemp.[4] Essentially this method consists of absorption in a special recirculating absorber by a spray of a buffered neutral solution of potassium iodide and estimation of the iodine by an electrometric method. During absorption it is necessary to use a light-tight box to prevent photochemical oxidation of the potassium iodide. Crabtree recommends sodium thiosulfate for titration but because of the dilute solutions an electrometric end point instead of starch is employed.

In the Naval Shipyard determinations, light was excluded from the sampling bubblers by wrapping them in black paper. NO_2 and H_2O_2 interferences were re-

* Available from Mine Safety Appliances Co., Pittsburgh, Pa.

moved by using $KMnO_4$ in the first glass bubbler of the train. Control samples were run on air uncontaminated with ozone from the process for the same length of time as the air samples. Samples and controls were analyzed at the same time and the samples were corrected for these control blanks.

There is a considerable possibility of errors in all ozone analyses because of the difficulty of obtaining a completely uncontaminated sample. The effect of oxides of nitrogen in high concentrations would be significant and it is difficult to expect results to agree closer than 25% because of such interferences. Many of the high values reported in the past can also be attributed to the fact that potassium iodide in solution is photochemically oxidized to iodine in the presence of sunlight. This occurs in both neutral and alkaline solutions.

LABORATORY AND FIELD TESTS

In the laboratory a series of 5 tests were conducted as outlined below. Air samples were collected during each welding operation as described. These experiments were conducted in a test room of large volume (greater than 50,000 cu ft) with a high ceiling used for welding and cutting developments. All windows were closed and only natural ventilation was available. The operator wore a welding helmet with protective glass for the inert-gas-shielded metal-arc conditions.

Test No. 1. Aluminum welding with 43S alloy wire $^3/_{32}$ in. in diam. Fillet welds in 1-in. thick 2S aluminum alloy were made. Helium was used as the inert-gas shield at a flow rate of 40 cfh. The welding current was 300 amp. The wire feed rate was about 160 ipm.

Test No. 2. This test was identical with No. 1 except that the inert-gas shield used was argon at 30 cfh and the wire feed was about 125 ipm.

Test No. 3. Aluminum welding with 2S alloy wire $^3/_{32}$ in. in diam on a 2S alloy base plate 1 in. thick as in Test No. 1. Helium was used as the inert-gas shield at a rate of 40 cfh. The welding current was 300 amp and the feed rate about 160 ipm.

Test No. 4. Stainless steel welding with Type 316 alloy wire, $^1/_{16}$ in. Type 347 alloy base plate $^1/_2$ in. thick. This was flat stock, the bead was laid horizontally from a vertical position. Argon was used as the inert-gas shield at 30 cfh. The welding current was 290 amp and the wire feed rate was 230 ipm.

Test No. 5. This was the same as No. 4 except that helium was used as the inert-gas shield at 30 cfh.

Single, high-volume samples were taken throughout the welding period in Tests Nos. 1 through 4. Several hand-pump samples and gas samples were taken during each test. It is important to note that the welding period was so short that hand-pump sample volumes were limited, making it difficult to perform a chemical analysis on them. The pleated samples, however, were adequate since air sampling was at a high rate, 70 to 75 cfm.

In taking the gas samples, 300-ml Pyrex glass pipets with stopcocks at each end were preceded by a paper filter holder unit to remove particulate material to prevent its interference in the gas analysis. Gases were flushed through the sample flasks by a rubber aspirator bulb (75 ml capacity). A clean filter paper was inserted before gas samples were obtained in each test. This paper was subsequently examined for stain appearance.

The field tests consisted of welding in a welding bay of the Sheetmetal Shop. This room with a volume of 5700 cu ft is partitioned, and the opening of 7 ft between ceiling and top of partition leads into the main workroom of the Sheetmetal Shop which has a volume of approximately 300,000 cu ft. Aluminum joints, varying in length from 24 to 51 in., were welded on an iron table. Operators wore personal protective clothing and helmets. A flexible exhaust, connected to a manifold removed the fumes and gases from the working zone when required in these studies. Samples were obtained with and without exhaust ventilation. It was observed that the welder could place the flexible duct fairly close to the arc (within 4 to 6 in.) without interfering with the inert-gas shield. This distance, of course, will vary dependent on the linear air velocity through the flexible duct opening, and the rate of flow of the argon or helium gas.

RESULTS

The results of fume and gas samples obtained in the laboratory study are shown in Tables 1, 2, 3 and 4. Table 1 gives fume concentrations and component analyses from the high-volume pleated filter samples. Table 2 presents results obtained from the hand-pump filter samples and Table 3 shows the data obtained for oxides of nitrogen and phosphine gas concentrations.

A study of Table 1 indicates that total fume concentrations range from 5.0 to 27.9 mg per cubic meter. Highest concentrations were obtained during aluminum welding. These samples were analyzed for aluminum, aluminum oxide and silicon dioxide. The sample collected from Test No. 3, in which a silicon-free alloy wire was used, was analyzed for aluminum. The remainder of the sample was assumed to be aluminum oxide. The analysis showed that only a small amount of metallic aluminum was present. The inert atmosphere present during inert-gas-shielded metal-arc welding apparently does not prevent the finely divided aluminum fume from immediate conversion to the oxide on contacting the atmosphere surrounding the inert-gas shield. Since the amount of aluminum found in the sample from Test No. 3 was practically negligible, samples from Tests No. 1 and No. 2 were only analyzed for silicon dioxide and aluminum oxide. It is interesting to note that these two samples agree well in aluminum oxide and silicon dioxide percentages. This agreement might be expected since the only difference during the welding operations was the different gases used for inert shields.

It is interesting to note in Table 1 an apparent enrichment in the silicon dioxide composition of the fume.

Table 1—Inert-Gas-Shielded Metal-Arc Welding Process,* Total Fume Concentrations and Analyses

Samples Collected by High Volume Air Sampler[1] in
Apparatus Research Laboratory, Air Reduction Co., Murray Hill, N. J.;
No Exhaust Ventilation

Test No.	Operation	Sample: Rate mean, cfm	Time, min	Volume: cu ft	Volume: cu m	Weight, mg	Concentration of total fume, mg/cu m	Analyses, %: Al	Al_2O_3	SiO_2	CrO_3
I	Aluminum welding, 43S alloy wire 3/32 in., 2S alloy base 1 in., He, 40 cfh, 300 amp, wire feed 160 ipm	70.0	6.10	427	12.1	146.5	12.1	...	88.5	11.5	...
II	Aluminum welding, 43S alloy wire 3/32 in., 2S alloy base 1 in., A, 30 cfh, 300 amp, wire feed 125 ipm	72.6	6.09	442	12.5	63.1	5.0	...	87.7	12.3	...
III	Aluminum welding, 2S alloy wire 3/32 in., 2S alloy base 1 in., He, 40 cfh, 300 amp, wire feed 160 ipm	60.0	6.55	393	11.1	309.4	27.9	0.035	99.965	...	...
IV	Stainless steel welding Type 316 alloy wire 1/16 in., Type 347 alloy base 1/2 in., A, 30 cfh, 290 amp, wire feed 230 ipm	69.6	8.13	566	16.0	129.0	8.1	...	...	...	0.004

* Aircomatic method.

The composition of 43S alloys contains 5% Si and the volatilization is differential and produces slightly more than twice as much SiO_2 as predicted from stoichiometric relationships.

The amount of water-soluble hexavalent chromium was determined in the sample from Test No. 4 obtained during stainless steel welding. Chromic anhydride is apparently only present in small amounts indicating that most of the volatilized chromium is not in the hexavalent state.

The hand-pump samples shown in Table 2 were not analyzed for aluminum oxide since insufficient material was present. Unfortunately the only direct method for analysis of aluminum oxide requires much larger

Table 2—Inert-Gas-Shielded Metal-Arc Welding Process,* Fume Concentrations Obtained from Hand-Pump Samples

(Same conditions as Table 1)

Test No.	Sample No.†	Welding material	Inert gas	No. of strokes	Volume, cu m	Concentration, mg/cu m
I	1	43S-Al	He	40	0.024	Faint stain visible by transmitted light only
I	2	43S-Al	He	38	0.023	
I	3	43S-Al	He	48	0.029	
I	1G	43S-Al	He	100	0.008	Strong brown-gray stain visible by reflected light
II	4	43S-Al	A	41	0.025	Faint gray stain visible by reflected light
II	5	43S-Al	A	88	0.053	Moderate gray stain visible by reflected light
II	2G	43S-Al	A	80	0.006	Faint gray stain visible by reflected light
III	6	2S-Al	He	49	0.029	Faint stain visible by transmitted light only
III	7	2S-Al	He	42	0.025	
III	3G	2S-Al	He	140	0.011	Strong gray stain by reflected light
IV	8	No. 316 Stainless	A	10	0.006	150 (Fe)
IV	9	No. 316 Stainless	A	10	0.006	9 (Fe)
IV	10	No. 316 Stainless	A	15	0.009	6 (Fe)
IV	11	No. 316 Stainless	A	15	0.009	1 (Fe)
IV	12	No. 316 Stainless	A	2	0.001	450 (Fe)
IV	13	No. 316 Stainless	A	3	0.002	25 (Fe)
IV	4G	No. 316 Stainless	A	110	0.011	9 (Fe)
V	14	No. 316 Stainless	He	15	0.009	8 (Fe)
V	15	No. 316 Stainless	He	3	0.002	250 (Fe)
V	16	No. 316 Stainless	He	20	0.012	75 (Fe)
V	5G	No. 316 Stainless	He	100	0.008	3 (Fe)

* Aircomatic method.
† Number samples taken by 600-ml hand pump. Number samples followed by letter G were collected by filter unit in front of gas samples (Table 3) using 75-ml squeeze bulb.

amounts than were obtained. It was observed, however, that a visible stain formed on the filter paper which could be used as a basis for preparing visible standards for analyzing aluminum welding fume concentrations.

Total fume concentration from the stainless steel welding was determined by means of standards which had been previously prepared in our laboratory from coated electrode welding fumes. These samples vary widely, ranging from 1 to 450 mg. per cubic meter. It should be pointed out that these results can only be considered as approximate since they were compared with standards prepared from coated electrodes rather than inert-gas-shielded metal-arc welding. Since the amount of hexavalent chromium was low in the pleated filter sample taken in Test No. 4, no significant amount could be found in the samples obtained on filter paper stains presented in Table 2.

Results of the gas samples are shown in Table 3. These samples all were analyzed for oxides of nitrogen by means of the phenoldisulfonic acid method with the exception of Sample No. 3 which was analyzed for phosphine as outlined above.

Table 3—Inert-Gas-Shielded Metal-Arc Welding Process,* Oxide of Nitrogen Concentrations

(Same conditions as Table 1)

Test No.	*Sample No.*	*Welding material*	*Inert gas*	*Volume, ml*	*Concentration, ppm as NO_2*
I	1	43S-Al	He	330	0.0
I	2	43S-Al	He	317	0.4
II	4	43S-Al	A	310	0.4
II	5	43S-Al	A	318	0.2
III	6	2S-Al	He	333	3.3
III	7	2S-Al	He	283	1.2
IV	8	No. 316 Stainless	A	260	0.1
IV	9	No. 316 Stainless	A	253	0.8
V	10	No. 316 Stainless	He	265	0.0
V	11	No. 316 Stainless	He	255	0.0
I	3	43S-Al	He	322	0.0†

* Aircomatic method.
† Sample analyzed for phosphine (arsine, stibine).

The concentrations of oxides of nitrogen range from 0 to 3.3 ppm and no detectable phosphine was present. Table 4 presents data obtained from separate tests conducted for ozone determination. Samples were collected as close to the arc as possible; in front of the operator's helmet in the working zone and from inside the helmet by withdrawing the sample from a tube located near the operator's face. The concentrations inside the helmet are lowest due to breathing dilution as mentioned previously and because of oxidation reaction with various materials and contaminants.

Field results are presented in Tables 5 and 6. The data in Table 5 correspond to conditions in which no local exhaust was employed, while those in Table 6 are based on two types of exhaust hoods.

Table 4—Inert-Gas-Shielded Metal-Arc Welding Process*

(Same conditions as Table 1)
Ozone Concentrations

Test No.	*Operation*	*Location*	*Ozone concentration, ppm*
	A. Aluminum welding		
1	Aluminum welding—shield gas 80% He, 20% A at 50 cfh, 195 amp	Sample tube 2½ in. from center of arc. Sample collection time 38 sec	4.3
2	Same as 1	In operator's working zone *outside* helmet 10 in. from arc. Sample collection time 38 sec	1.1
3	Same as 1	In operator's breathing zone *inside* helmet 10 in. from arc. Sample collection time 40 sec	0.53†
4	Same as 1	Same as No. 3. Sample time 91 sec	0.72†
	B. Stainless welding		
1	Stainless welding on flat plate with multiple passes—shield gas, argon 30 cfh, 200 amp	Sample tube 2½ in. from center of arc. Sample collection time 60 sec	2.4
2	Same as 1	Same as 1	2.6
3	Same as 1 except 1% O_2 added to shield gas	Same as 1	2.5
4	Same as 3	In operator's working zone *outside* helmet. Sample collection time 60 sec	0.96
5	Same as 3	In operator's breathing zone *inside* helmet. Sample collection time 30 sec	0.39

* Aircomatic method.
† Row of low-velocity air jets within welding helmet.

The first type was a simple flexible exhaust hose located near the operation and adjusted by the operator to suit welding conditions. The second, a movable flanged lateral exhaust hood with an opening width covering the length of the largest size piece welded.

It can be seen in Table 5 that oxide of nitrogen concentrations without exhaust are comparable to those obtained in the laboratory study (Table 3). Ozone concentrations are higher in the field study which may be attributed to general room ventilation conditions and the length of welding period. This same observation also applies to the total fume concentrations since longer samples are involved in a more confined volume in the field study.

The results shown in Table 6 indicate a marked reduction in oxides of nitrogen, ozone and total fume samples when exhaust ventilation is applied. The simple flexible exhaust hose is not as effective as the lateral hood and concentrations close to the arc are not significantly affected.

Total fume control is much more effective with the lateral exhaust hood because a greater control area is provided.

DISCUSSION

In any discussion of welding processes (inert-gas-shielded metal-arc included) it is important to consider that the major health hazards depend on the type of metal welded, thus with aluminum alloys the possible exposures are to aluminum oxide and silicon dioxide fumes, ozone and oxides of nitrogen. With stainless steel alloys, possible exposures include hexavalent chromium, iron fume, ozone and nitrogen oxides.

Table 5—Inert-Gas-Shielded Metal-Arc Welding Process—Gas and Fume Concentrations

Unventilated Operations Field Data from New York Naval Shipyard

Oxides of Nitrogen

Sample No.	*Operation*	*Location*	*Nitrogen dioxide concentration ppm*	*Mean ppm*
1	Welding aluminum joints	Operator's working zone. Welding bay sheet metal shop. Near arc	7.8	
2			5.4	
3			4.3	5.8
4	Same as above	Operator's working zone outside helmet	2.3	
5			2.5	
6			2.5	
7			2.3	2.4

Ozone

Sample No.	*Operation*	*Location*	*Sample time, min*	*Welding time, min*	*Ozone concentration Mean, ppm*	*Mean during welding, ppm*
1	Same as above	Operator's working zone outside helmet	41	30.2	1.5	2.1
2			41	26.8	1.5	2.3
3			42.5	27.4	1.1	1.7

Total fumes

Sample No.	*Operation*	*Location*	*Sample time, min*	*Welding time, min*	*Fume concentration Mean, mg/cu m*	*Mean during welding, mg/cu m*
1	Same as above	Operator's working zone outside helmet	34.6	11.0	37.5	117.6
2			15.4	9.5	32.5	52.8

Table 6—Inert-Gas-Shielded Metal-Arc Welding Process—Gas and Fume Concentrations

Ventilated Operations Field Data from New York Naval Shipyard

Oxides of nitrogen

Sample No.	*Operation*	*Location*	*Nitrogen dioxide concentration Ppm*	*Mean ppm*
1	Welding aluminum joints. Flexible exhaust duct	Operator's working zone. Welding bay, sheet metal shop. Near arc	7.0	
2			3.8	
3			3.1	4.6
4	Same as above. Flexible exhaust duct	Operator's working zone outside helmet	0.5	
5			1.1	
6			0.9	0.8
7	Same as above but with movable flanged lateral exhaust hood connected to a movable duct. Baffled hood also located near floor	Operator's working zone outside helmet	0.8	
8			0.5	
9			0.6	
10			0.6	
11			0.5	
12			0.5	0.6

Ozone

Sample No.	*Operation*	*Location*	*Sample time, min*	*Welding time, min*	*Ozone concentration Mean, ppm*	*Mean during welding, ppm*
1	Welding aluminum joints. Using movable flanged lateral exhaust above	Operator's breathing level	67.3	42.5	0.15	0.24

Total fumes

Sample No.	*Operation*	*Location*	*Sample time, min*	*Welding time, min*	*Fume concentration Mean, mg/cu m*	*Mean during welding, mg/cu m*
1	Welding aluminum joints. Flexible exhaust duct	Operator's working zone outside helmet	16.3	11.5	16.3	23.1
2	Welding aluminum joints as above but with movable flanged lateral exhaust duct and baffled hood near floor	Same as above	23.0	9.7	3.6	8.5
3	Same as above	Same as above	17.0	7.8	2.8	6.2
4	Same as above	Same as above	17.8	10.4	1.7	2.8

Fumes

The toxicity of aluminum oxide fumes has not been clearly established. There have been reports of lung damage to workers in aluminum powder manufacturing in Germany[11] but these have not been substantiated by studies of propeller grinding operations in England.[12] In the United States, alumina fume exposures exist in alumina abrasives manufacture where the exposure is simultaneous with that of silicon dioxide fumes. This mixed exposure is equivalent to fumes from welding 43S or other aluminum-silicon alloys. The British experience[13] and recent observations in the use of corundum[14] (calcined alumina) indicate that uncomplicated alumina exposures are not harmful.

On the other hand, Gärtner[15] studied corundum workers engaged in smelting operations. He concludes that the substance which causes damage is really mullite (sillimanite) possibly in combination with amorphous silica. Hagen[16] in a recent study of workers breaking up blocks of corundum found a significant number of cases with lung damage and noted that these men also had exposures to furnace fumes. Modder and Schmitt[17] report damage to several workers' lungs in an aluminum smelter.

Exposures in the U. S. alumina abrasive industry[18] have resulted in several deaths in men exposed for periods of three to ten years. There have been no definite opinions whether the injury was due to fine silica or alumina or a combination of the two. Fume concentrations found in plants manufacturing alumina abrasives range from $7\frac{1}{2}$ to 120 mg per cubic meter of total fume with silica (SiO_2) percentage varying from 16 to 54 and the alumina (Al_2O_3) varying from 35 to 65%.

It has been noted before that while the concentration of silicon in 43S alloy is only 5%, in the welding fume the concentration is nearly 12%. This is similar to results observed in aluminum abrasive fume exposures[19] where bauxite, which contains only 4 to 8% silicon dioxide, shows as much as 20 to 50% silicon dioxide in the ambient fume. Apparently there is differential dispersion and the fume is enriched with silica.

One other study may be mentioned since it was an attempt to simulate the alumina abrasive exposures. MacFarland and Hornstein[20] studied rats and guinea pigs exposed to inhalation of pure alumina, pure silica and alundum furnace fumes for six months. The lungs when examined indicated that the silica damage was comparable to other silica dusts. The alumina and mixed alumina and silica (alundum) exposures produced a marked chronic interstitial reaction comparable to the industrial cases.

We have made a few preliminary studies in the Harvard laboratories, within the last three years, on alumina-silica catalysts as used in the catalytic cracking process of the petroleum industry. Results to date indicate that this amorphous aluminum-silicate dust can cause fibrosis in tissue. Preliminary studies with only powdered alumina indicate no reaction. These, however, are preliminary tests and have not advanced to the stage where permissible workroom concentrations can be established.

It would therefore seem advisable, in view of the questionable nature of such exposures, to establish a limit for aluminum welding fume concentrations with any arc-welding process which would not be injurious. After many years experience in the South African gold mines on weight sampling and counting of dust, it was found that if the free silica concentration was kept below 1.0 mg per cubic meter, new cases of silicosis did not appear. In this country[21] we used a permissible concentration value of five million particles per cubic foot (light field count) which is roughly the equivalent of 0.1 mg per cubic meter (for 1μ particles*). It is also known that smaller particle sizes produce fibrosis more rapidly but, conversely, they are not as readily retained.[22] Silica fume is, of course, the finest form of this material and has sizes ranging from 0.01 to 0.3 μ.

As a basis for controlling the fume from 43S and similar alloys, we feel that total fume concentrations should be maintained so that SiO_2 fume concentrations reaching the welder do not exceed 0.1 mg per cubic meter. Where alumina fume occurs with silicon as an impurity in the welding materials it would seem desirable to keep concentrations below 15 mg per cubic meter. The latter figure is to avoid nuisance and irritation problems.

Fume concentrations in stainless steel welding indicate that there is very little hexavalent chromium present and, on a basis of the allowable concentration of 0.1 mg per cubic meter for CrO_3, a much higher total fume concentration than was obtained would be permissible. It was recognized by most observers that the rapid rate of metal deposition made it unlikely that operators could continue welding for long periods to create such high concentrations. The amount of heat to be dissipated in a rapid welding process in confined spaces is as important as the amount of fume generated. It appears that the limit of 15 mg per cubic meter for total iron fume will be adequate for stainless steel inert-gas-shielded arc welding.

Material Balance Study

In order to compare the inert-gas-shielded metal-arc welding process with coated electrodes, on the basis of fume production, a material balance was made. Results on electrodes were obtained from previous studies made at the Harvard laboratory. Data on the inert-gas shielded metal-arc process were obtained at the Apparatus Research Laboratory.† These results are summarized at the top of the next page.

It is apparent from this data that this inert-gas-shielded metal-arc process, in spite of higher current densities, does not consistently produce as much total fume as coated electrode welding. Some of this differ-

* Theoretical conversion 0.1 mg of 1 μ spheres of quartz equals 2 million particles per cubic foot = mppcf, assuming a standard deviation of unity.
† Obtained by H. R. Fisher and his colleagues at the Air Reduction Co.

Type of operation	Fume in % of electrode weight
1. Electrodes' ($^5/_{32}$- to $^1/_4$-in. coated) optimum current density on black iron	4.0
2. Electrodes' ($^5/_{32}$- to $^1/_4$-in. coated) optimum current density on galvanized iron	9.0
3. Aircomatic-stainless steel No. 316, $^1/_{16}$-in. wire and No. 347 base plate, argon shield	0.5*
4. Aircomatic-aluminum 28 wire and base	
Argon shield	3.0*
Helium shield	1.9*

* Includes some spatter loss.

ence may be due to the size and rate of feeding the electrode and some to oxidation loss. Reduced loss of material lessens the amount of atmospheric fume contamination.

Gases

Gas concentrations indicate that the amounts of oxides of nitrogen present in the inert-gas-shielded metal-arc process sampled are well below the permissible limit of 25 ppm of total oxides (ASA standard). In spite of the higher current densities, the action of the gas shield and the size of the arc do not produce as high oxide of nitrogen concentrations as bare rod stick welding. The concentrations in Tables 3, 5 and 6 are comparable to values obtained for coated-rod arc welding. The oxides of nitrogen values given in Tables 5 and 6 are nitrogen dioxide and represent what is considered as the most toxic component of the total oxides. These figures are well within the lowest recommended limits even very close to the arc where high temperatures exist.

Ozone concentrations in the laboratory and field studies only exceed the suggested maximum allowable concentration of 1 ppm (American Conference of Governmental Industrial Hygienists, 1953[21]) near the arc and working zone. Inside the welder's helmet in his breathing zone, concentrations are below this value. The odor detected near the working zone is classified as "chlorinelike." Thorp[23] reports that pure ozone from 0.1 to 2 ppm has an odor similar to that of clover or new-mown hay, and from 2 to 10 ppm it has a garliclike odor. The presence of small amounts of oxides of nitrogen changes the odor and it is called chlorine or ammonia by various observers at 0.1 to 5 ppm. Above 5 ppm the odor is acid in character. Pure ozone is apparently much less toxic than ozone and oxides of nitrogen mixed, which is the usual case, hence a limit of 1 ppm is set for the latter case. Apparently the nitrogen oxides exert a synergistic effect with ozone.

It should be borne in mind that ozone is quite unstable and that ozone and oxides of nitrogen react rapidly. The kinetics of this reaction are discussed by Johnston and Yost.[24] It will suffice here to point out that the reaction is nearly completed in a few seconds and may be computed from rate constants presented by these authors.

The zone which appears to be the most important to control is not at the arc but in the welder's breathing zone. The ozone concentrations in the working zone for coated electrodes and inert-gas-shielded metal-arc welding do not differ materially as the summary presented in Table 7 shows, but appear to depend more upon the metal excited. These determinations were made at the same time as the inert-gas-shielded metal-arc studies for comparative purposes. From these data it is evident that manual aluminum welding produces the highest inert-gas-shielded metal-arc working and helmet concentrations, but they are not significantly different from those of the coated electrodes commonly used. Of course, electrode welding at lower current densities produces much less ozone as other investigators have shown.

Table 7—Ozone in Welding Atmospheres

Method	Arc current, amp	Mean ozone concentrations, ppm: At arc	Working zone	Inside helmet
Inert shield (aluminum)				
Automatic (4 ft from arc)	180	6.1	0.3	0.17
Manual (10 in. from arc)	195	4.3	1.1	0.63*
Manual	192	14.0	3.2	0.54
Inert shield (stainless)				
Manual (8 in. from arc)	200	2.5	0.96	0.39
Manual	200	1.5	1.5	0.50
Standard electric arc (mild steel, 18 in. from arc)				
Coated electrode, $^3/_{16}$ in.	220	2.1	...	0.72
Bare electrode, $^3/_{16}$ in.	220	3.6	...	0.57

* Row of low-velocity air jets within welding helmet.

CONTROL OF EXPOSURES IN INERT-GAS-SHIELDED METAL-ARC WELDING

It is important to note here that inert-gas-shielded metal-arc welding is not any more hazardous than other welding procedures, but because its operational requirements and other features vary somewhat, different control requirements are needed.

As in all welding exposures the methods of control are similar. The limitations to continued welding in confined spaces are principally heat and irritating gas concentrations as pointed out previously by Viles and Silverman.[25] There are two methods of controlling the fumes and gases from arc welding. General or dilution ventilation can be supplied to the enclosure or welding space at such a rate that effective dilution of the fumes takes place and high concentrations cannot develop. This method, of course, is for the general environment but does not efficiently remove fumes near their source. The volumes of air necessary to effectively reduce the concentrations to which the operator is exposed, make general ventilation uneconomical except for other workers exposed in large shops or workrooms. Its

chief advantage for inert-gas-shielded metal-arc welding is that the air velocities created would not be sufficient to disturb the inert-gas shield. Obviously it would not be possible to appreciably dilute the gas and fume concentrations in front of the individual welders by this method.

More effective control is provided by the use of local exhaust hoods and, as shown in Table 6, it is possible to provide enough lateral exhaust with a flanged hood as described without disturbing the inert-gas shield. Typical hood application is shown in the uncontrolled arc, Fig. 2, and the controlled, Fig. 3. No visible fume is escaping in the second case. The correct distance can be determined by adjustment. Flexible exhaust ducts can control the exposure but do not cover sufficient area unless the operator continuously relocates the opening to maintain control.

In the event that local exhaust cannot be used it is recommended that an individual air supply to the welding hood be provided. The article by Eisenbud and Silverman[5] describes these devices and their effectiveness. They can also be used by welders who may exhibit individual sensitivity to welding gases.

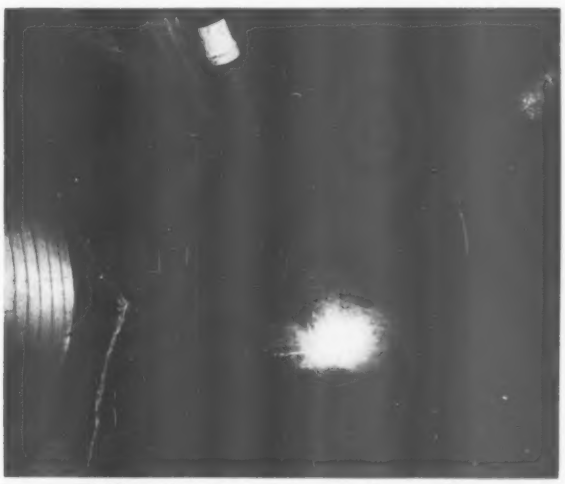

Fig. 2 Welding aluminum bronze without exhaust, using 1/16-in. wire, 235 amp, 24 v and 30 chf of argon

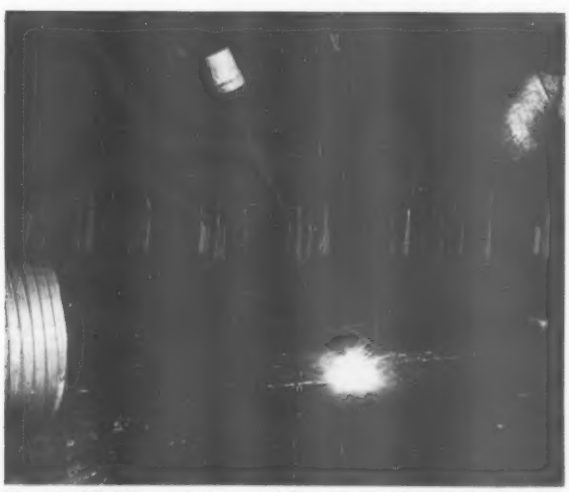

Fig. 3 Welding aluminum bronze with exhaust using 1/16-in. wire, 235 amp, 24 v and 30 cfh of argon

Reported symptoms of headaches, eye irritation and a "dry feeling" in the throat have been evoked in welding of aluminum with inert-gas shields. The first two reactions it is believed are due to improper eye protection as discussed below. The dry throat or upper respiratory irritation may be attributed to ineffective heat dissipation or fume and gas control.

Radiation Exposures

The protection of the welder from radiation is an important consideration in inert-gas-shielded metal-arc welding, particularly on aluminum fabrication. The correct glass composition for protection of the eyes is an important item. It is recommended that care be exercised to keep all exposed skin surfaces protected on head, hands and body. It is also desirable to avoid white or reflective garments that would increase the intensity of the light striking the eye protective filter glass. In addition, the high ultraviolet deteriorates fabrics rapidly making full leather jackets and gloves more economical for high-duty cycles.

The radiation exposures from inert-gas-shielded aluminum arcs are richer in certain bands (see Fig. 1) than those encountered in normal coated electrode welding operations. This is largely infrared energy at well above the usual electrode levels. Since the emissivities of the inert-gas-shielded arcs at the longer wave lengths have not yet been completely determined and since the transmission characteristics of the filter glasses used are not known quantitatively at these longer wave lengths, it was decided that the high factors of safety present in the use of filter glasses for eye protection would be maintained for operators using this welding technique and a study[26] was made in order to develop and specify a goggle having a greater absorbance of infrared radiation than present goggles, and one which would still provide equivalent transmission in the visible range. On a basis of this investigation it was found that the best protective glass is one composed of three layers finished to optical flatness as required by current Federal Specifications. These in order are as follows:

1. A sheet of clean heat-resistant glass possessing a transmission of 92% or better at all wave lengths from 400 to 750 mμ when compared with air. Its sole purpose is to protect the next two layers of glass from spatter.

2. A sheet of regular welding filter glass (such as Noriweld or Filterweld) meeting all current specifications for welder's goggles except that it shall be lighter by one shade number than it is desired to have the final assembled goggle. That is, if it is desired to make a Shade 10 goggle, a Shade 9 filter shall be used.

3. A third component in the glass series should have transmission characteristics equivalent to those of Corning Glass Co.'s Aklo* in the wave-length region

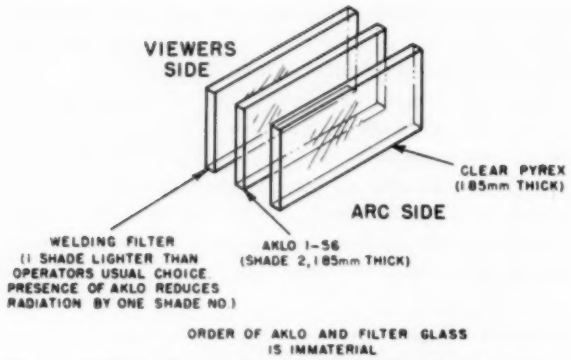
VIEWERS SIDE
CLEAR PYREX (1.85mm THICK)
ARC SIDE
WELDING FILTER (1 SHADE LIGHTER THAN OPERATORS USUAL CHOICE PRESENCE OF AKLO REDUCES RADIATION BY ONE SHADE NO.)
AKLO 1-56 (SHADE 2, 1.85mm THICK)
ORDER OF AKLO AND FILTER GLASS IS IMMATERIAL

Fig. 4 Schematic assembly of protective glass for inert-gas-shielded metal-arc welding

from 0.2 to 5μ. The thickness of this glass shall be such that its transmission of monochromatic light of wave length 510 mμ shall be 42.5% of that of air. The order of the second and third components is interchangeable. Figure 4 shows a schematic assembly of the final goggle.

Sizes are not specified since they can be made to fit various types of goggles.

Many of the complaints in inert-gas-shielded metal-arc welding operations have been largely headaches and watery eyes, and are believed attributed to in adequate eye protection. Some rapid clothing deterioration has also been reported, which is also due to the same exposure.

It should be realized that the specifications for added eye protection with the inert-gas-shielded metal-arc welding process is a temporary measure that may become permanent or may be dropped on completion of further studies to determine the need for the high infrared absorption characteristics of the present standard filter glass combinations.

CONCLUSIONS AND RECOMMENDATIONS

1. On a basis of this study it is apparent that inert-gas-shielded metal-arc welding does not create any unusual health hazards. Fume and gas concentrations are not markedly different from conventional electrode welding.

2. Ozone concentrations are greatest near the arc and exceed permissible limits outside the helmet in the working zone if no control is applied. Ozone concentrations inside the helmet are below recommended limits due to dilution and ozone decomposition.

3. Inert-gas-shielded metal-arc welding produces higher ozone concentrations on aluminum than on stainless steel but no difference could be noted between argon- and helium-shield gases.

4. Stainless-steel inert-gas-shielded metal-arc welding does not produce significant hexavalent chromium concentrations. Iron fume should be controlled within specified limits for elimination of visibility loss and nuisances (15 mg per cubic meter).

5. The fume hazard from silicon alloy welding of aluminum is proportional to the amount of silicon present. Silica fume concentrations exceed permissible limits with 5% silicon-aluminum alloys unless local ventilation is used. As a tentative limit in the light of present knowledge on the effects of mixed alumina and silica exposures, it is recommended that total fume concentrations be maintained below 15 mg per cubic meter for aluminum alloys where silicon is only an impurity. The total fume concentration when silicon is present should be such that the silica concentration does not exceed 0.1 mg per cubic meter.

6. Oxides of nitrogen concentrations are below allowable limits even in the absence of ventilation. The inert-gas shield, reactions with fumes, and ozone prevent abnormal air concentrations in the working zone.

7. When it is desirable to control fumes and gases from arc welding, the use of lateral exhaust ventilation is the most efficient. Care must be taken in its use, however, to avoid disturbing the inert shield. Fume concentrations with the inert-gas-shielded metal-arc welding process appear to be independent of the type of shielding gas.

8. There is no basic reason to believe that the general specifications for arc welders' filter glass which are ordinarily used in coated electrode welding do not give adequate protection against long-wave infrared although a more highly absorbent infrared glass combination is recommended at present until an adequate quantitative study is made. The recommended composition for satisfactory control is given in this article.

9. Operators sensitive to gaseous products of welding, such as ozone, should use local exhaust supplemented by air supplied helmets if necessary.

10. In terms of deposited material, inert-gas-shielded metal-arc welding produces less fume per unit weight of electrode wire used when compared to coated electrode arc welding.

* *Glass Color Filters*, by Corning Glass Works, p. 19. Copyright 1948.

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(*Continued from page 217-s*)

Table 1—Average Mechanical Properties of Nickel-Iron Arc-Welded Joints in Ductile Iron

Plate condition	*Postweld. HT**	*Yield strength, 0.2% offset, psi*	*Tensile strength, psi*	*Elongation in 2 in., %*
As-cast	AW	56,900	65,000	1.5
As-cast	FHT	45,900	63,800	10
As-cast	FT	47,700	61,600	5
Annealed (FHT)	AW	44,500	60,600	9
Annealed (FHT)	FHT	42,500	60,500	12
Annealed (FHT)	FT	44,700	60,700	13

* AW = as-welded: FHT = 1650° F-1-FC + 1275-5-AC; FT = 1275° F-5-AC

The data show the value of welding ductile iron in the annealed condition and also the effect of postheat treatment. A few commercial applications involving the use of the nickel-iron electrode are shown in accompanying illustrations.* Incidentally the nickel-iron electrode was also used for all repairs of casting defects in the Ductile Ni Resist and ductile iron castings used in the above-mentioned jet aircraft engines.

It should also be pointed out that repair welding by the oxy-acetylene process, using ductile iron filler, is being accomplished satisfactorily by a number of foundries.

We agree with the authors that brazing offers a useful method for joining of ductile iron. There are no doubt numerous fields of application where brazing can be profitably employed to obtain some of the metallurgical advantages suggested by the authors. Further work in this field is needed.

In summary, field experience suggests that, when suitable precautions are observed which take into account the special characteristics of the material, fusion welding of ductile iron is commercially practicable. In fabrication welding of ductile iron, care should be taken to design the structure so that welds will be located in least critical areas with respect to service stresses. This is, of course, common practice in design of welded structures.

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* See THE WELDING JOURNAL, **32** (10), 953 (Figs. 10 and 12) and 954 (Fig. 13) (1953).

Authors' Reply

The authors wish to thank Mr. Kihlgren for his discussion of their paper. He presents some interesting examples of commercial applications of welded ductile iron.

Unquestionably, ductile iron can be satisfactorily welded in many applications where design factors are favorable and especially when the weldment can be given a full heat treatment both before and after welding. However, such high-temperature heat-treating cycles are a severe limitation on large-scale structural joining owing to the high costs involved.

In addition, the authors wish to point out that all the physical properties presented by Mr. Kihlgren were obtained from transverse tensile tests, which can hardly be expected to give realistic results when the welded joint contains zones of appreciably different strength and ductility. More realistic data should be obtained from longitudinal bend tests and impact tests.

A restatement of our conclusions will make clear that there is no fundamental disagreement between our point of view and that of the discusser.

1. A joining process for nodular iron that carries any part of the base stock over the eutectic temperature will result in microstructural changes detrimental to the physical properties of the joint.

2. The impairment of properties will be severe if continuous carbide networks form and persist.

3. Graphitizing heat treatment will not completely restore the properties because of unfavorable size, shape, and distribution of the graphite in the HAZ.

4. This impairment under current joining practice must be taken into account, either by locating joints in regions of low stress or, if this is impossible, by derating the design stress of the structure.

5. Fruitful avenues for further research are:

(*a*) Development of a brazing process that can be conducted entirely below the eutectic temperature of the stock and that uses a filler metal of adequate strength and ductility.

(*b*) Development of a welding process that yields proper graphite structure in the weld and HAZ.

High-Pressure Vessels Subjected to Static and Dynamic Loads

Elastic stress-strain relations for external and internal walls for cylindrical shells subjected to internal pressure. Comparison of actual stresses to theoretical stresses. Application of various theories of fracture. Suggestions for design formulas

by E. D. Narduzzi and Georges Welter

Abstract

The following is a condensed version of a comprehensive research work carried out by E. D. Narduzzi, as partial fulfillment of the requirements for a Master of Applied Science Degree. This investigation is the first of its kind to initiate a series of more advanced studies concerning the behavior of thick-walled pressure vessels. After having overcome all technical difficulties naturally involved in an experimental research work dealing with quite exceptional pressures up to 50,000 psi, it was possible to test a small number of models of various dimensions statically and dynamically. Fatigue tests were initiated and conclusive results in this field have not yet been obtained.

It is felt that the opportunity for continuing this work is excellent now that the groundwork has been thoroughly prepared in the form of building special machinery and organizing the testing procedure.

This investigation was carried out in the Strength of Materials Laboratory of the École Polytechnique Institute, Montreal, Quebec, Canada.

Synopsis

THE purpose of this investigation was to study the mechanical properties of various materials to be used in the manufacture of vessels resisting very high internal pressures while being subjected to static and dynamic loads at room temperature. No attempt was made to determine the distribution of stresses in the fully plastic range. The determination of the mechanical properties included elastic stress-strain relation and yield and ultimate strengths for the models and test bar specimens. The walls of the cylinder under pressure were subjected to a state of triaxial stress of unequal magnitude. For thick-walled vessels the Lamé equation can predict fairly accurately both the yield and ultimate strength for vessels fabricated from ductile material. According to these experiments, the tangential strain and stress of the internal wall are always greater than those of the external surface. Furthermore, both wall surfaces yielded at the same time with the external wall surface only developing longitudinal cracks before rupture. Indentations or material imperfections are potential origins of fractures. Thick walled vessels show brittle fractures with little deformation while thin-walled vessels fracture by shearing with large deformation. Flat heads on vessels increase the yield and the ultimate strength of comparatively short shells.

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Georges Welter, Ph.D., Director of Research and Professor of Applied Mechanics, École Polytechnique, University of Montreal, Montreal, Quebec, Canada.

INTRODUCTION

The tendency in the chemical and oil industries to operate processes under higher and higher pressures has forced the engineer to devote more attention to the design of pressure vessels. This of course demands superior steels and better fabrication methods in order to cut down weight and over-all cost. Economy and safety are the requisites of a balanced design.

This investigation deals with the determination of actual stresses in the elastic range at the internal and external walls of a vessel as well as the yield point and the ultimate strength. These stresses are compared with test bar values (Table 1) obtained from specimens cut tangentially from the shell as shown in Fig. 3.

This rather short work cannot be conclusive and the importance of further experimentation on static and fatigue tests is emphasized to determine the endurance limit stress which can finally lead to the reduction of the present permissible safety factors.

THEORETICAL ANALYSES

Many analytical methods of pressure vessel design have been proposed in an attempt to approach closer to actual conditions.

Table 1—Test Bar Results

DESCRIPTION	VESSEL No.			
	1	2 & 3	4	5*
Material	A.S.T.M. A-7	A.S.T.M. A-106, Gr.B	S.A.E. 1030	A.S.T.M. A-201, Gr.A F.B.Q.
Elongation	35.0 %	25.6 %	31.5 %	28.5 %
Reduction in Area	48.0 %	49.5 %	51.5 %	---
Stress at Yield Point	29,650 psi	35,500 psi	36,000 psi	41,060 psi
Ultimate Stress (Original Area)	60,950 psi	69,050 psi	75,000 psi	59,735 psi
Ultimate Stress (Final Area)	117,100 psi	136,200 psi	155,000 psi	---

* Obtained from Mill Test Reports.

The following theories of failure of elastic action were used for comparison with experimental results (Tables 4 and 5):

1. The Maximum Principal Stress Theory.
2. The Maximum Strain Theory.
3. The Maximum Shearing Stress Theory.
4. The Maximum Strain Energy Theory.

Table 2—Vessel Fabrication

VESSEL No.	DESCRIPTION
1	Seamless shell machined from ASTM A-7 bar stock material $4^1/_4$ in. diam
2	Fabricated from ASTM A-106, Grade B schedule No. 160 seamless steel pipe 6 in. diam. The pipe was not machined at either inside or outside wall surfaces
3	Fabricated from ASTM A-106, Grade B schedule No. 140 seamless steel pipe 8 in. diam. The pipe was concentrically machined at inside and outside wall surfaces
4	A one-piece seamless shell and head machined from a SAE 1030 forged steel billet (Fig. 2)
5	Vessel constructed in accordance with the ASME Code for UPV, 1949 Edition, with welding to paragraph U-69 and stress relief. Shell was pressed hot in two halves with two longitudinal double, butt-welded joints ground flush inside and outside (Fig. 3)

Table 3—Physical Dimensions of Vessels

DESCRIPTION	VESSEL No.				
	1	2	3	4	5
Outside Diameter- O.D.	3.246"	6.771"	8.574"	9.480"	9-1/2"
Inside Diameter - I.D.	2.002"	5.345"	6.966"	5.000"	8"
Wall Thickness - t	0.622"	0.713"	0.804"	2.240"	3/4"
Ratio - I.D./t	3.23	7.5	8.66	2.23	10.67
Inside Length - I.L.	11"	23-1/2"	23-1/2"	21-1/2"	24"
Ratio - I.L./I.D.	5.49	4.40	3.38	4.30	3.00
Deformation beyond the Yield Point	Large	Small	Small	Very Small	Very Large

The direct application of most of these equations to the design of pressure vessels is questionable as experimental results obtained differ from theoretical values. The graphs, Figs. 9 and 10, show experimental and theoretical results. It can be seen that the curves of experimental results are located between those given by both thin cylinder formulas and are much higher than those determined by the well-known theories (Curves *6* to *9*, inclusive).

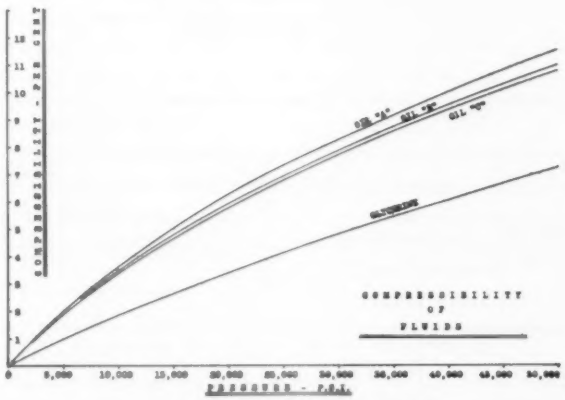

Fig. 1 Compressibility of fluids

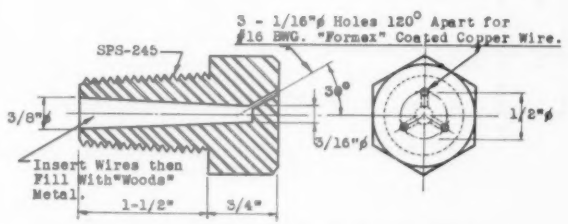

Fig. 2 Plug for internal strain gage outlet

STATIC TESTS

Industrial and model pressure vessels have been statically tested to rupture by several experimenters[1–7] mainly to determine the safety of a particular design. These vessels having a ratio of inside diameter to the wall thickness greater than 16 could be classified as "thin wall"; the internal pressure rarely exceeded 5000 or 6000 psi.

In this investigation static tests

Table 4—Experimental Stresses

VESSEL No.	Internal Pressure p.s.i.		STRESSES CALCULATED FROM STRAINS					
			Internal Surface			External Surface		
	Elastic Limit	Yield Point	S_t	S_L	S_s	S_t	S_L	S_s
1	14,500		--	--	--	18,000	9,600	4,600
		16,000	--	--	--	19,800	10,600	5,100
2	9,000		--	25,100	--	26,200	13,500	6,400
		9,500	--	26,600	--	27,600	14,200	6,800
3	8,300		53,400	23,500	15,900	31,400	14,780	8,500
		8,800	56,700	24,750	16,870	33,900	15,450	9,000
4	21,000		67,500	23,500	22,000	12,600	6,800	3,400
		26,500	Large	34,500	32,400	20,000	10,000	5,000
5	5,000		30,700	20,000	5,500	29,500	16,500	6,700
		6,200	38,000	24,300	6,900	37,900	21,500	8,100

NOTATION: S_t - Tangential Stress
S_L - Longitudinal Stress
S_s - Shear Stress

Table 5—Comparison of Theoretical Stresses

VESSEL NO.	INTERNAL PRESSURE p.s.i.			TANGENTIAL STRESSES										SHEAR STRESS
				INTERNAL SURFACE						EXTERNAL SURFACE	THIN CYLINDER			
				STRENGTH THEORY					CLAVA-RINO Formula	LAME Formula	1 Using Ins. Rad.	2 Using Mean Rad.	3 ASME Code UPV	INTERNAL SURFACE
	Elastic Limit	Yield Point	Rupture	A	B 1	B 2	C	D						
1.	14,500			32,500	36,800	34,100	47,000	38,500	34,300	17,700	23,400	30,600	32,000	23,500
		16,000		35,900	40,700	37,700	52,000	42,500	37,800	19,600	25,800	33,800	35,390	26,000
			28,000	62,700	71,000	65,900	90,700	74,400	66,200	34,200	45,200	59,100	61,900	45,400
2.	9,000			38,500	41,100	36,800	47,600	42,100	36,800	29,500	33,800	38,200	39,200	23,800
		9,500		40,600	43,500	38,800	50,200	44,500	38,800	31,200	35,600	40,400	41,400	25,100
			17,000	72,800	77,600	69,500	90,000	79,700	69,500	55,800	63,900	72,100	74,000	45,000
3.	8,300			41,100	43,500	38,600	49,600	44,500	38,600	32,800	36,000	40,100	41,000	24,800
		8,800		43,600	46,200	41,000	52,400	47,100	41,000	33,800	38,200	42,500	43,500	26,200
			15,100	75,000	79,100	70,200	90,400	81,000	70,400	59,600	65,500	73,000	74,550	45,100
4.	21,000			37,200	43,500	39,800	58,400	48,100	41,100	14,000	23,400	34,000	80,900	29,200
		26,500		47,000	55,000	50,300	73,600	60,600	52,000	20,400	29,600	42,900	102,000	36,800
			50,000	88,500	103,800	94,800	139,000	114,800	98,000	33,400	55,600	81,000	192,500	69,500
5.	5,000			29,300	30,800	27,100	34,200	31,200	27,200	24,300	26,700	29,200	22,250	17,100
		6,200		36,300	38,200	33,600	42,400	38,700	33,700	30,000	33,100	36,200	27,600	21,200
			10,000	58,600	61,600	54,300	68,400	62,400	54,400	48,600	53,400	58,400	44,500	34,200

NOTATION: A - Maximum Principal Stress Theory (Lamé)
B - Maximum Strain Theory - 1) Longitudinal Stress Omitted
2) Longitudinal Stress Included
C - Maximum Shearing Stress Theory
D - Maximum Strain Energy Theory.

were carried out at room temperature under much higher internal pressure reaching in one case a maximum of 50,000 psi. The load was applied by means of a 60,000-lb Baldwin testing machine acting on a small piston and cylinder arrangement designed with check valves for repeated strokes (Fig. 11). The hydraulic medium used was a light-grade lubricating oil which proved to be as good as high-grade dielectric transformer oil. Fluid pressure was applied in small increments and strain and deformation readings were taken at each increment as far as possible beyond the yield point.

Due to the fact that it was important to dispose of the fluid medium with a minimum of compressibility at such high pressure, especially in view of a great number of repeated loadings as in the case of fatigue tests, preliminary compression tests with various fluids as glycerin and oil were carried out. The results of these tests made under pressures up to 50,000 psi are shown in Fig. 1.

FATIGUE TESTS

Two machines were designed and fabricated for this project to test vessels to destruction under dynamic loads:

1. The Twin Arrangement Pulsator with an average pressure of 5000 to 20,000 psi can give by changing the pistons, cylinders and stroke a maximum pressure of 50,000 psi and 50 to 100 cpm. This machine is suited for testing vessels with thin- and medium-walled thickness under repeated load.

2. A completely automatic air-operated hydraulic pump arrangement with a maximum capacity of 20,000 psi and the number of cycles depending on the pressure and the volume of oil to be displaced. This arrangement is best suited for large vessels and can also be used for applying direct static loads.

INSTRUMENTATION

All vessels tested in this project were equipped with

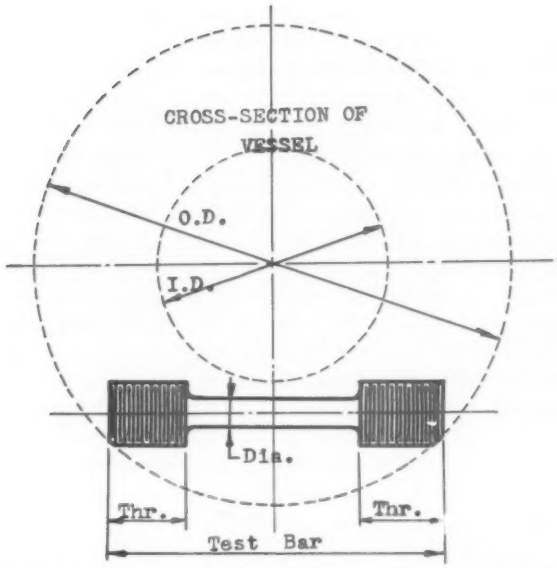

Fig. 3 Test bar specimen cut tangentially from shell of vessel

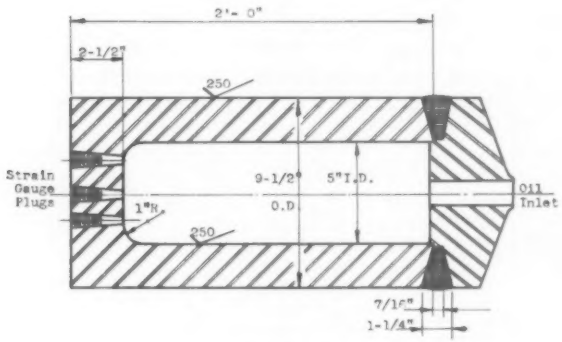

Fig. 4 Detail of vessel 4

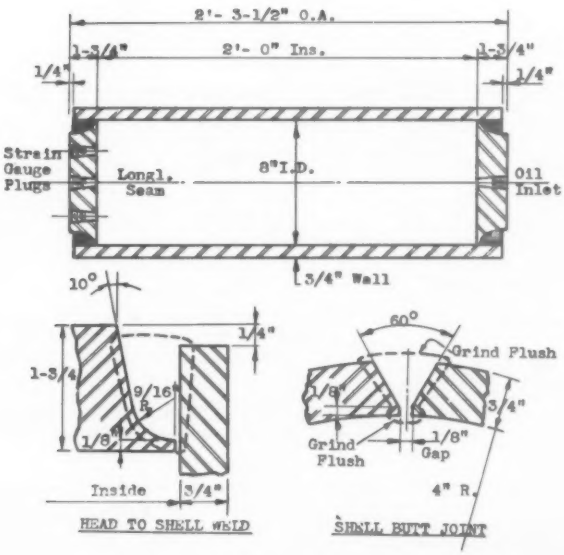

Fig. 5 Detail of vessel 5

the following strain and deformation measuring devices:

1. Standard A-1, SR-4 as well as PA-1, Post Yield strain gages and rectangular rosettes located on the internal and external walls.
2. Huggenberger extensometers on the outside wall.
3. Dial gages for outside diameter and circumference deformation readings.

The internal strain gages were installed first, then the remaining flat head was welded in place. During the welding operation these gages were protected from excessive temperature by cooling water applied on the outside surface as well as by circulating air through the vessel.

What at first appeared to be a major problem of extracting leakproof, yet electrically insulated, leads from internal strain gages, was very simply solved by using plugs as shown in Fig. 2. During preliminary tests these proved satisfactory under repeated oil pressures as high as 60,000 psi. The "Formex" coated copper wire No. 16 B.W.G. used, received a perfect bond with the slow cooling "Woods" metal poured into the conical chamber. The plug threads when coated with a plastic seal and securely tightened in place withstood without leakage the above-mentioned hydraulic test pressure.

RESULTS AND OBSERVATIONS

The strain readings for all instruments on each vessel were carefully plotted and interpreted. The stresses calculated from these strains were compared with theoretical values as shown by Tables 4 and 5.

During these high-pressure tests it was observed that general yielding of the shell occurred simultaneously for the internal and external walls in both the tangential and longitudinal directions contrary to expected differences (Figs. 6 and 7).

In thick-walled vessels the internal surface strains were much greater than the external strains. Very small deformations were observed in the shell wall up to the breaking point. In thin walled vessels where the internal and external strains did not vary appreciably, large deformations of the shell wall resulted in a considerable reduction of thickness. Further, it was observed that strains obtained from gages located at 45 deg to the principal axes (as in rectangular rosettes) always gave values which ranged somewhere in between the longitudinal and circumferential strains. For the internal surface of a vessel these strains apparently approached the magnitude of the tangential strain as the wall thickness increased.

It is estimated that the increase in longitudinal strain for the external surface at a stress near the yield point of the material is shown somewhat greater than it would be. The vessels used were rather short and the end restrictions due to the flat heads caused the shell to bow, which, as far as the external longitudinal strain is concerned, introduces an additive tensile strain. This, however, cannot be regarded as the

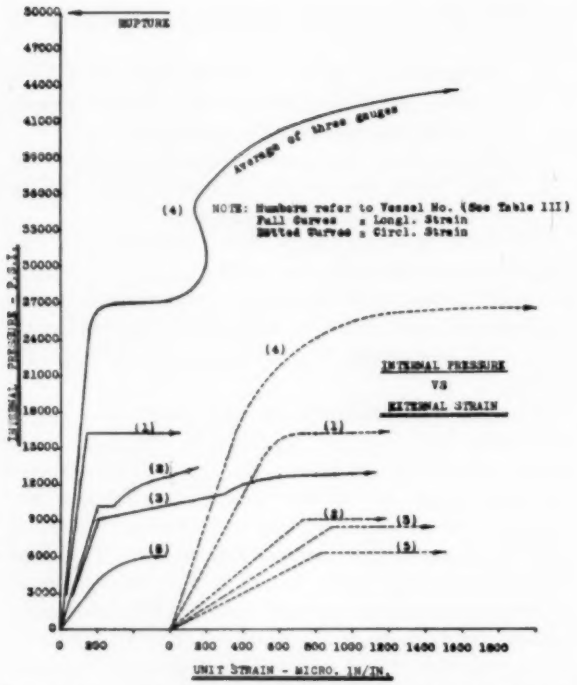

Fig. 6 External longitudinal and circumferential shell strains

cause of the sudden increase of the longitudinal strain because it was equally observed for Vessels 2, 3, and 4, which had different thicknesses and very little deformation.

Another peculiarity is, that, at the yield point the calculated internal tangential stress is higher than the ultimate strength of the material for Vessel 4, close to the ultimate for Vessel 3 and almost equal to the yield point stress for Vessel 5. It is evident that this is a factor depending on the wall thickness and the internal diameter of the vessel. In all cases the tangential yield stress at the external surface was below the yield point stress of the material. External longitudinal and circumferential stresses were found to be in the right proportion to each other, that is, in the ratio of one to two respectively. It was noted also that the shear stress for the internal surface appeared to shift toward the value of longitudinal stress as the wall thickness increased (Fig. 8).

For thick-walled vessels even though the stress at the inside surface may be very high, the material may have reached the plastic range while the external surface is still below the yield point stress. The inside surface is prevented from cracking by the compressive force of the understressed outer shell. The radial stress which may have reached the yield point value is probably also responsible for preventing cracking on the inside surface since it is a compressive stress.

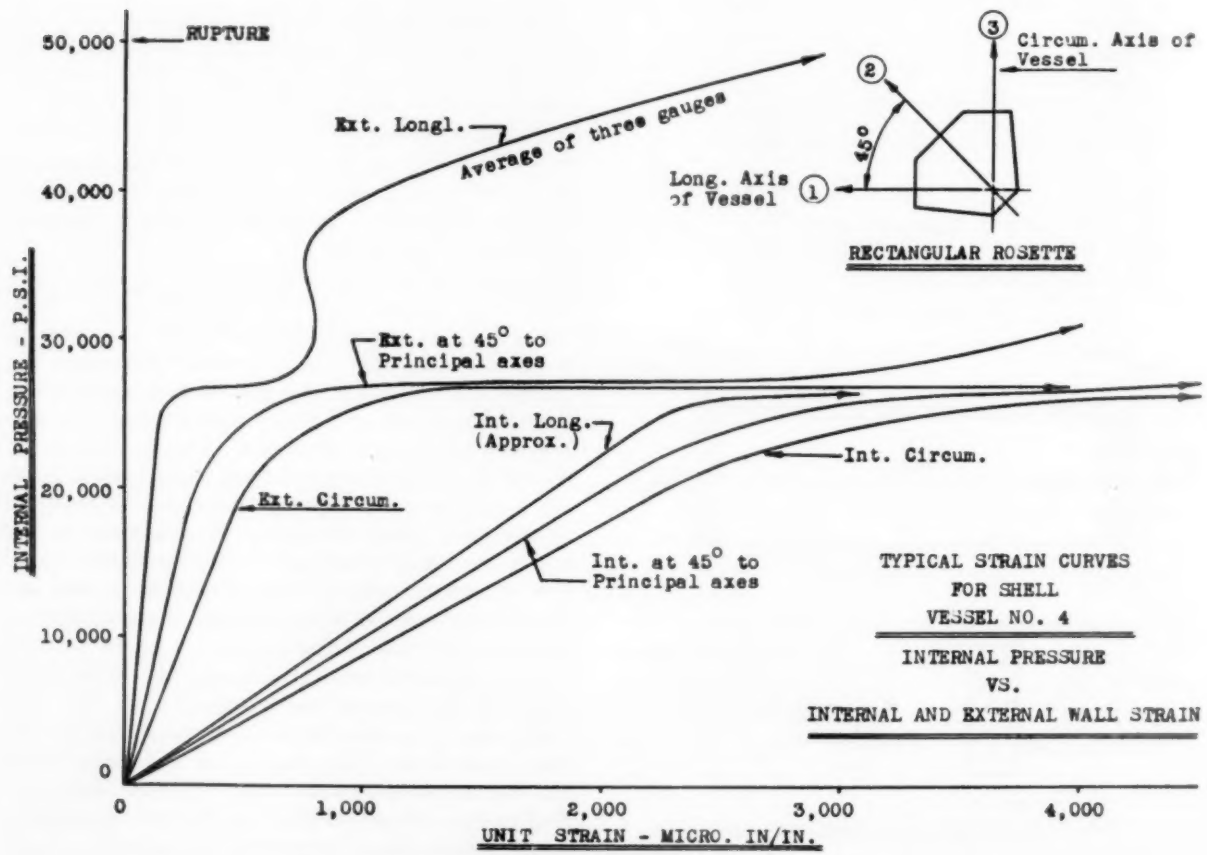

Fig. 7 Typical shell strains for internal and external wall surfaces of vessel 4

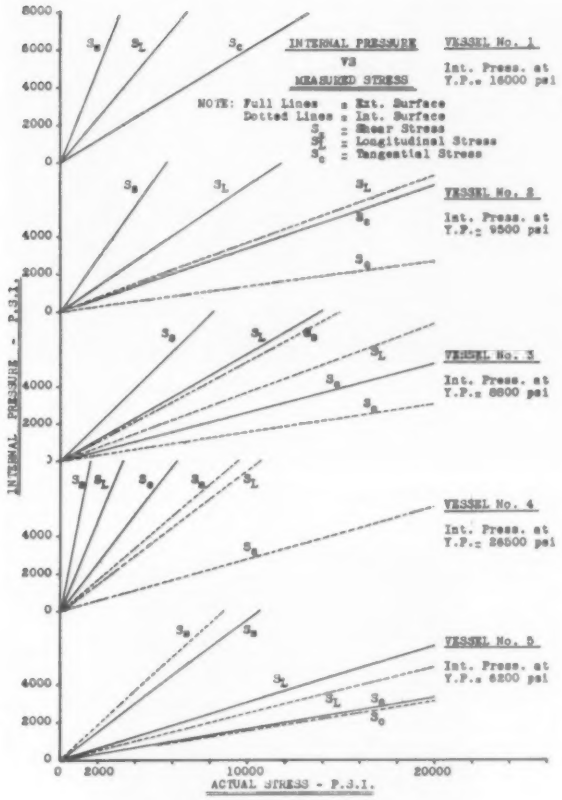

Fig. 8 Experimental stresses

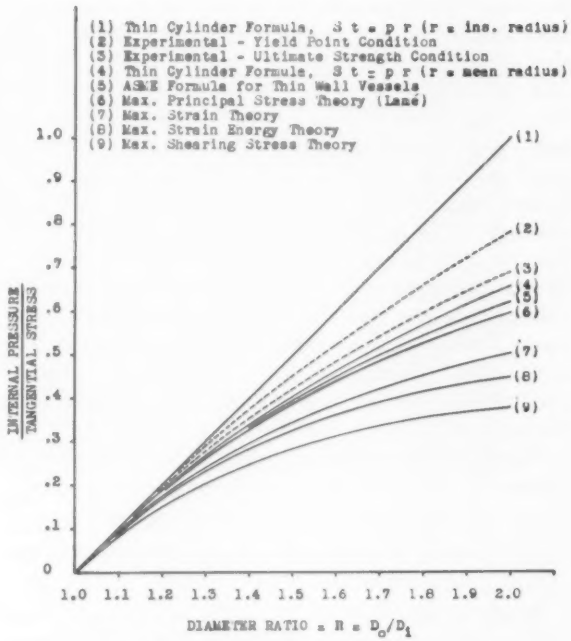
(1) Thin Cylinder Formula, S t = p r (r = ins. radius)
(2) Experimental - Yield Point Condition
(3) Experimental - Ultimate Strength Condition
(4) Thin Cylinder Formula, S t = p r (r = mean radius)
(5) ASME Formula for Thin Wall Vessels
(6) Max. Principal Stress Theory (Lamé)
(7) Max. Strain Theory
(8) Max. Strain Energy Theory
(9) Max. Shearing Stress Theory
INTERNAL PRESSURE / TANGENTIAL STRESS
DIAMETER RATIO = R = D_o/D_i
1.0
1.1
1.2
1.3
1.4
1.5
1.6
1.7
1.8
1.9
2.0

Fig. 9 Comparison of stress curves

However, the condition of stress which exists in the cross section of the wall of a vessel cannot be explained readily accordingly to the above-mentioned results. The limiting conditions of maximum and minimum

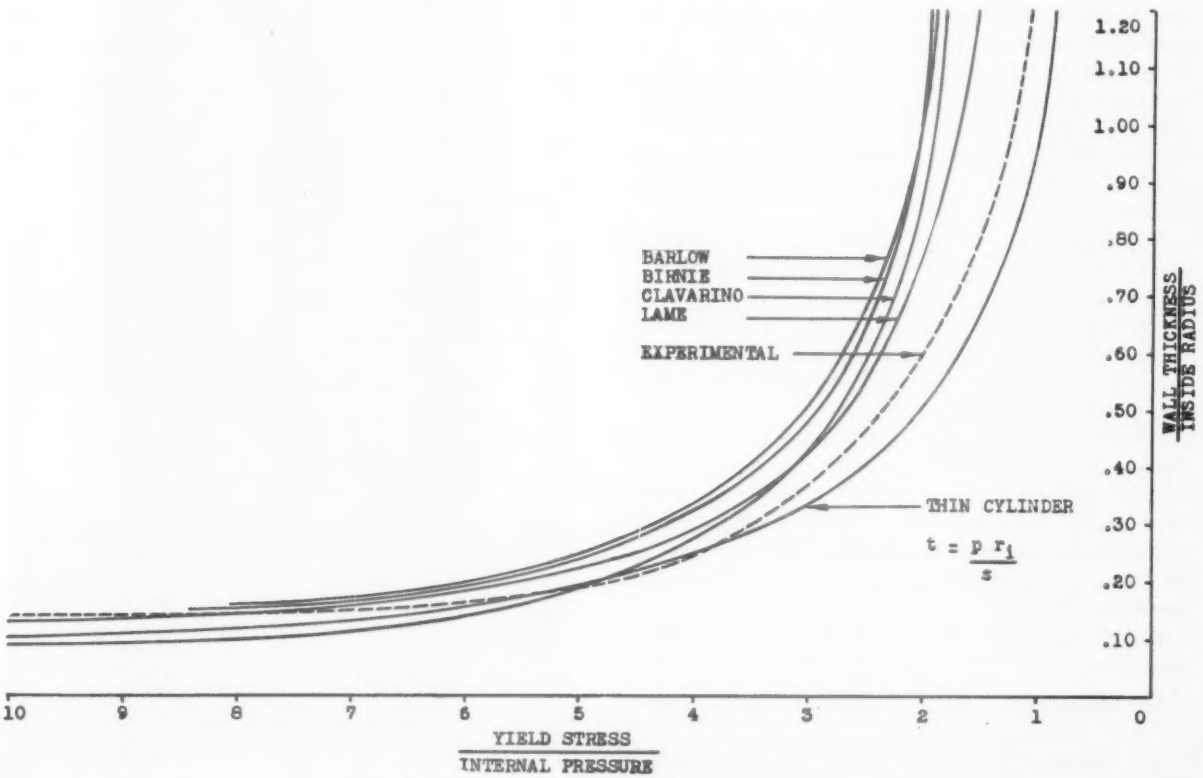

Fig. 10 Comparison of various formulas[3]

stress can be calculated for the inside and outside surfaces and in between the stress must have some intermediate value which will not follow the common concept presented by the Lamé equation. In thick-walled vessels the tangential stress for the external surface may reach a value equal to the yield stress of the material only after considerable deformation while the internal surface may not be very far from the ultimate strength. The fact that the graphs for Internal Pressure vs. Internal and External Tangential Strains show these two surfaces to yield together might mean that the external surface stress rises instantaneously to and beyond the yield point value once this general yielding is reached. Having reached this stage it is quite possible for the whole cross section to act plastic throughout.

Experimental results for tangential stress when compared with Lamé's formula were found to be as follows:

(*a*) For the yield point condition about 22% higher.
(*b*) For the ultimate strength condition only about 13% higher.

It can be said that the Lamé formula is rather conservative, it approaches closely to the actual stresses calculated from strains and may be applied satisfactorily for the design of thick-walled pressure vessels.

The above statements are opposite to the findings of earlier investigators[3] who claim that based on testing full-size thick-walled vessels the maximum ultimate pressures obtained were 80 to 85% of those predicted by Lamé formula. It was previously found also that for full-size vessels, presumably built to code regulations, with shell lengths only twice the diameter the ultimate pressure was increased by 13 to 14%. Since the average length of model vessels tested was roughly four times the internal diameter it can be assumed that the rigidity due to the flat heads is noticeable in elevating the yield and ultimate values of the internal pressure for vessels with a shell length of four times their diameter.

An examination of thick shells just before rupture (Vessel 4) revealed many longitudinal cracks at the outside surface (Fig. 13). After failure, however, inspection of the internal surface failed to disclose any such cracks which might have closed under zero pressure. It seems likely that cracks in thick-walled pressure vessels start from the outside.

Fractures for thick vessels were brittle in nature with the tendency to shatter into fragments at maximum internal pressure. The behavior of thin vessels differed in that fractures started out as shear failures at about 45 deg and then ramified as brittle cracks. In all cases, at rupture, thin-walled vessels remained in one piece (Fig. 12).

For all vessels the causes for failures could be attributed to:

(*a*) Weakness in the material itself due to impurities or laminations.
(*b*) Surface defects such as punch marks or other indentations which may have been located

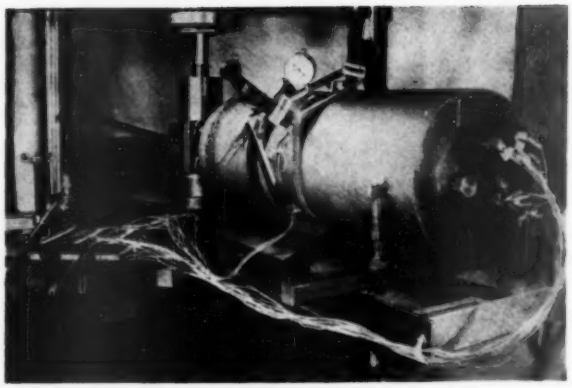

Fig. 11 Vessel 4 ready for testing

Fig. 12 Vessel 5 ruptured

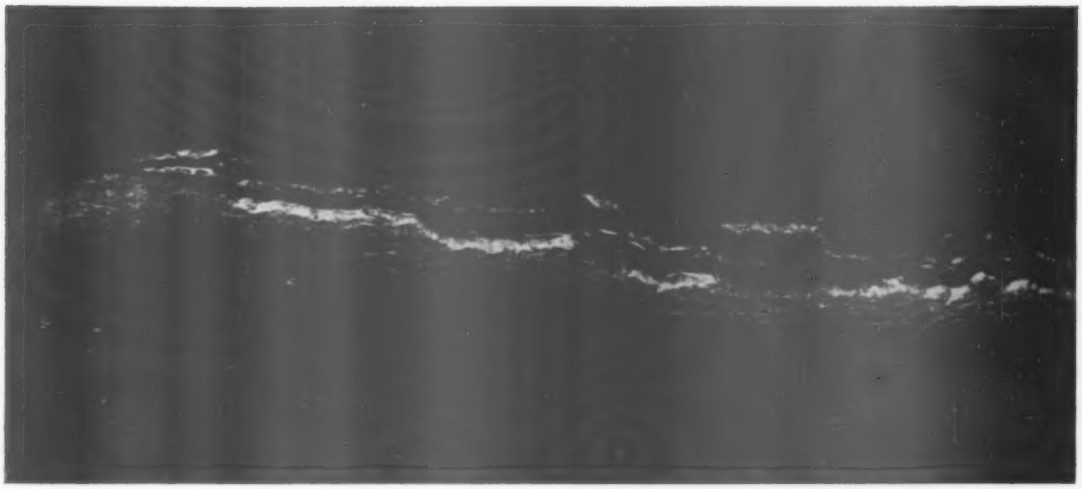

Fig. 13 Enlarged view of longitudinal cracks on external surface of vessel 4

in either internal or external walls (Fig. 12).

(*c*) Stress concentration.

The models fabricated with flat heads (Fig. 5) behaved satisfactorily for static tests, but proved inadequate for fatigue tests as failure invariably took place at the highly stressed root of head to shell circumferential weld. The flat heads were replaced later by semi-elliptical heads.

CONCLUSIONS

In spite of the limited number of vessels that could be tested under this program and the various sizes and materials chosen for economic reasons, the following results have been obtained:

1. *Thick-walled vessels*
   - (*a*) The deformation at fracture is not as obvious as that seen in thin-walled vessels fabricated from the same ductile material.
   - (*b*) The fractures are brittle in nature.
   - (*c*) Somewhere between the yield point and the ultimate value of internal pressure, the outside surface of the vessel develops longitudinal cracks. Since no such cracks were found in the interior surface it is conceivable that fractures may originate at the external surface. This should be verified by further experimentation. In industry, thick solid walled vessels should be examined periodically for external surface irregularities and put out of service if cracks are detected. (In view of the development of cracks in the external surface of such vessels, it can be said that multilayer vessels are safer and give better performance since each thin layer, to some extent, acts independently of one another and does not form cracks.)
   - (*d*) Theoretical stresses agree with actual values for the external surface only. For the yield point condition the internal tangential stress is much higher than the yield point stress of the material; the external tangential stress is smaller than the yield point stress.
   - (*e*) The shear stress at the internal surface approaches the magnitude of the longitudinal stress as the wall thickness increases.
   - (*f*) The Lamé formula predicts both yield and ultimate pressures with sufficient accuracy for ductile materials.
   - (*g*) At the yield point both the longitudinal and circumferential strains increase sharply.
2. *Thin-walled vessels*
   - (*a*) Vessels fabricated from ductile materials show large deformation before rupture occurs.
   - (*b*) Fractures show shear failures with large reduction in thickness.
   - (*c*) Such vessels do not shatter except in sub-zero temperatures.
   - (*d*) As in thick-walled vessels, both the tangential and longitudinal strains increase sharply at the yield point.
   - (*e*) The stress difference measured between internal and external surfaces is so small that it can be neglected.

Some explanation about the above findings may be given as follows:

The material in a pressure vessel wall does not behave in the same manner as in a simple tensile test specimen with lateral reduction in area (Poisson's Ratio). At some critical point in the wall of the vessel under pressure the material cannot flow under the strains imposed and consequently fracture takes place in a brittle fashion. The wall of a pressure vessel is subjected to three unbalanced stresses:

1. The tangential tensile stress indicating a maxi-

mum at the inner surface and a minimum at the external surface.

2. The longitudinal tensile stress which may be assumed constant throughout the cross section of the wall.
3. The radial compressive stress which equals the internal pressure at the internal surface and is zero at the external surface.

These observations point to the radial stress as playing a major part in the behavior of pressure vessels as the wall thickness increases. It is this stress which seems to be responsible for brittle fractures in thick-walled vessels.

Regardless of shell thickness all vessels showed that both the internal and external surfaces yielded at the same time even though the internal tangential stress was much greater than the external tangential stress when this occurred. The material on the inside reached the quasi-plastic state before the material on the external surface but cannot flow until the entire cross section has reached the plastic limit. Upon reaching this point general yielding takes place.

The stiffening effect of heads on the straight shell of a vessel is appreciable. Thick flat heads welded on short vessels increase the rigidity of the shell. It is estimated that the length of the shell should be at least five or six times the diameter where the D/t ratio is below 16, an arbitrary chosen dividing line between thick- and thin-walled vessels.

The experimental curves plotted for comparison with theoretical values (Fig. 9) may be expressed in the form of simple approximate equations.

1. For the yield point condition, Curve *2*, where the internal pressure at yield divided by the yield point stress of the material taken from a test bar specimen is plotted against the ratio $R = b/a$,

$$P = S(R^{7/6} - 1)$$

2. For the ultimate strength condition, Curve *3*, where the internal pressure at rupture is divided by the ultimate strength of the material taken from a test bar specimen is plotted against the ratio $R = b/a$,

$$P = S(1.1R^{5/6} - 1)$$

To this experimental curve we can apply a second equation as follows,

$$P = S \ln R$$

where ln is the natural logarithm to the base e and P, S and R are as given above.

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Stresses for Pressure Vessels and Boilers Up to 650° F

Calculation and rupture theory for boilers and pressure vessels at temperatures up to 650° F and subject to internal pressure

by W. P. Kerkhof

1. INTRODUCTION

THE differences of opinion regarding allowable stresses and weld efficiencies for boilers and pressure vessels are very great. In the United States the allowable stress is one-quarter of the ultimate tensile strength, combined with a weld efficiency of 95, 90 and 80%, respectively, dependent on whether radiographic examination and/or stress relieving is to be carried out or not. In Great Britain the same stress is permitted, while the weld factor depends on a special classification. The first-class vessel, stress relieved and radiographed, has a maximum weld efficiency of 95%, the third-class vessel, nonradiographed and nonstress relieved, with very limited inspection, has a weld efficiency of about 60%. In Sweden an allowable stress of two-thirds of the yield point is tabulated, with weld efficiencies varying from 60% up to 90%. In the Netherlands the same stress is permitted with a weld factor of 100%, provided the calculated extra stress as a result of out-of-roundness of the cylinder is within certain limits. In this code the total stress, being the resultant of the membrane stress and the superimposed stress due to out-of-roundness, has a maximum allowable value, dependent on the yield point. If the stress due to out-of-roundness is large, the membrane stress must be correspondingly the same amount smaller, as the sum of both remains the same.

The result of all these different points of view is that the allowable stresses in pressure vessels vary from about 27% of the yield point up to 66%. It seems reasonable to look again into the matter, to establish which allowable stress and which weld factor can be used.

To avoid making this analysis too complicated, only service temperatures up to about 650° F are considered. The behavior of creep is thus not taken into account.

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2. ASSUMPTIONS

It is assumed that rupture occurs as a result of too high a stress or a combination of high stresses in two or three mutually perpendicular directions, or as a result of too high a strain. The true stresses occurring in a vessel are the sum of the so-called membrane stresses, the superimposed stress concentrations and the residual stress. It is a known fact that the magnitude of the residual stress has to be taken into account in the case of brittle rupture at subatmospheric temperatures and in the case of fatigue. It seems to be logical to assume that the residual stress is also important in the case of rupture of a pressure vessel at atmospheric and higher temperatures under static or semistatic loading. The expression "semistatic loading" is used here because an absolute static load does not occur in practice. Very small stress fluctuations will always be present due to what is called, in the Netherlands, "breathing" of the vessel.

Furthermore it is a fact that when rupture occurs in a pressure vessel during service, the appearance of the break is always brittle. A shear fracture has never been found. The following theory of rupture has been largely based on this phenomenon.

3. SIMPLIFICATIONS

To avoid complicating the analysis it is assumed that the material follows Hooke's law up to the yield point and that the amount of yielding which occurs is so small that no strain hardening takes place. In this case the stress-strain diagram has the form shown in Fig. 1. This assumption is not true for austenitic steels and nonferrous metals. For such materials another more complicated calculation has to be carried out, which can be done according to the same principle.

A second simplification has been made, assuming that the yield point of the weld is the same as that of the base metal. If necessary, the higher yield point of the weld can be taken into account but for a first attempt to show what happens in a pressure vessel wall, this complication seems unnecessary.

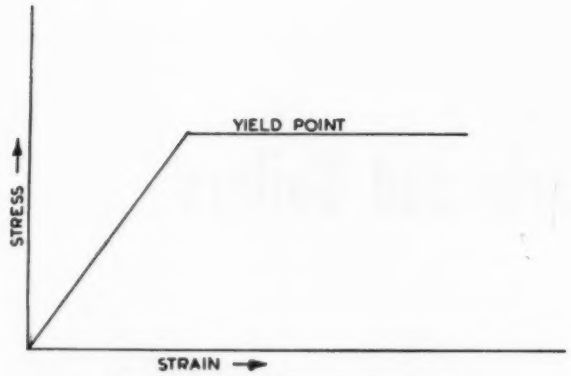


Fig. 1 Simplified stress-strain curve

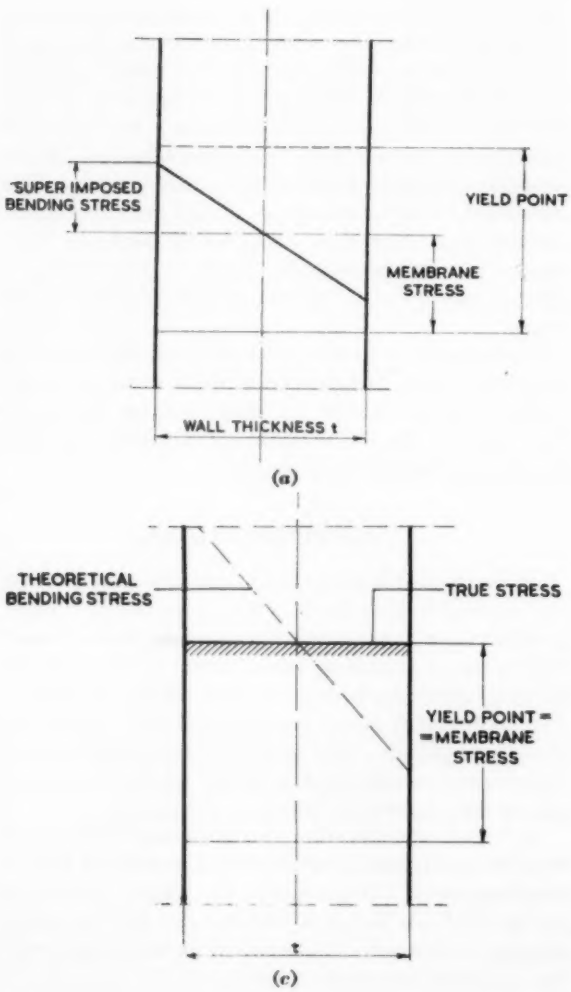
The third simplification is the assumption that if the state of stress is diaxial, yielding occurs when the maximum stress reaches the yield point of the tensile test. It is known that this is not quite true, but the difference as compared with the true behavior is small and, if necessary, this method of calculation can also be refined.

However, since on the following pages the calculations are only used to compare various requirements in the codes, a more accurate analysis is not necessary. For comparison only the above simplifications can be fully accepted.

4. STRESS CONCENTRATIONS

It is mentioned that stress concentrations have to be taken into account. Under the heading of stress concentrations are considered all stresses nonuniformly distributed over the plate thickness, and which have to be superimposed on the membrane stresses, except residual stresses. These stress concentrations can be subdivided into two groups:

(*a*) Stress concentrations caused by bending, acting in, at most, two mutually perpendicular directions: According to the simplification accepted above, these stresses cause yielding when the highest value of the stress reaches the yield point of the material in uniaxial tension. The magnitude of these stress concentrations is a function of the membrane stress or of the internal pressure of the vessel.



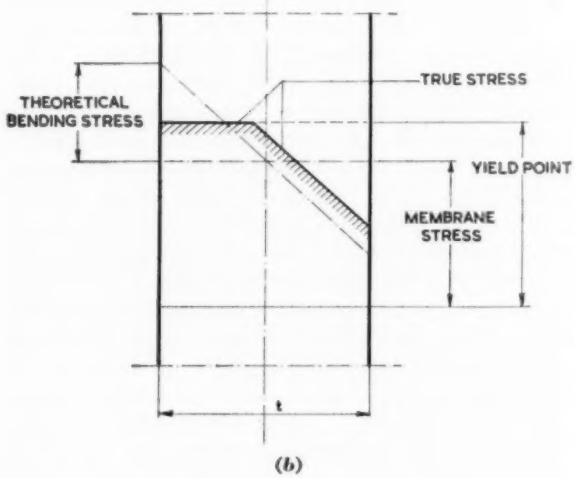


Fig. 2 The influence of the yield point on the stress distribution

The stresses are limited by the yield point of the metal. When the vessel wall yields over the whole thickness the stress concentration has vanished totally. Figure 2 shows such a stress concentration combined with a membrane stress, Fig. 2(*a*) in the elastic state, Fig. 2(*b*) when yielding occurs partially and Fig. 2(*c*) when yielding occurs over the whole thickness of the wall.

The magnitude of these stress concentrations varies always to a great extent along at least two mutually perpendicular directions.

Stresses due to out-of-roundness of the cylinder and extra bending stresses in domed ends belong to this type, provided the wall is not too thick. In thick-walled vessels the state of stress is triaxial. This entails that the above-mentioned simplification, that yielding occurs when the maximum stress is equal to the yield point, does not even approximately hold. Furthermore, when yielding occurs in thick walls, over the whole wall thickness, the stress is not uniformly distributed. The simplified stress diagrams shown in Figs. 1 and 2(*c*) cannot be used in this case.

(*b*) Stress concentrations having a parabolic character acting mostly in three mutually perpendicular directions: Such stresses occur near abrupt changes of shape, for instance near branch connections, manholes, supports and in fillet welds. The highest triaxiality occurs just below the surface of the material. As the three principal stresses are mostly tensile stresses, yielding occurs often at a much higher stress than the yield point in uniaxial tension. In this case it is quite possible that very high elastic stresses occur far above the yield point. As long as the stresses are fully elastic, the magnitude is a function of both the internal pressure and the shape of the detail.

When yielding occurs, however, the stress pattern alters from what it was in the elastic state, the distribution of the stresses, however, remaining nonuniform. If in this case the internal pressure is relatively low, yielding occurs over a small part of the material; if the internal pressure is relatively high, a greater amount of material yields. The magnitude of the maximum stress, however, remains about the same in both cases. When yielding occurs, the maximum stress is thus only dependent on the shape of the detail and nearly independent of the internal pressure.

An extreme example is formed by the stresses near a crack. Such stresses always reach a magnitude of 2.5 to 3 times the yield point, even if the load is very low.

Figure 3 shows the result of the calculation of the stresses around a crack having a width of one-third of the plate thickness t. The allowable membrane stress in the plate without cracks is assumed to be two-thirds of the yield point ($^2/_3Y$). The stress near the crack is $2.57Y$. The plastic zone is very small ($0.0585t$). If an allowable stress of $0.3Y$ were taken into account, the stress pattern should be the same. In this case the width of the plastic zone should be $0.022t$.

It is quite possible that the internal stresses due to welding near such details are also triaxial. As a result of the triaxiality it may be very difficult, if not impossible, to relieve these stresses by heat treatment or other methods.

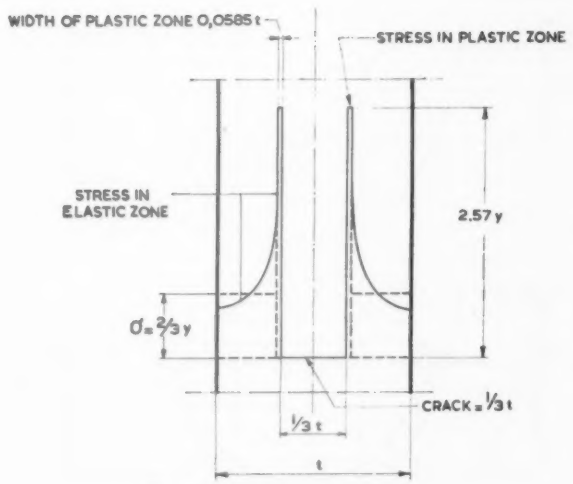


Fig. 3 Stresses near a crack

The stresses mentioned under *b* can only be very high as a result of bad design, bad manufacture and/or bad inspection (cracks). If the high stresses exist, however, they are nearly independent of the internal pressure, or of the magnitude of the theoretical allowable membrane stress.

Assuming initially that design and manufacture are satisfactory, only stress concentrations mentioned under (*a*) have to be considered.

5. RESIDUAL STRESSES

When residual stresses in a nonstress-relieved weld are calculated or measured in a direction either parallel or perpendicular to the weld, at some places values up to the yield point are always found. Such a high stress value has to be taken into account in the following analysis.

Recent measurements on nonwelded plates have shown that due to rolling and especially due to a nonuniform cooling down after rolling, residual stresses up to the yield point also occur. The high residual stress is thus not only caused by the welding but always exists in the plate, provided no stress relieving has been carried out.

Welded vessels are often stress relieved at 1200° F. After this stress relieving the residual stress cannot be much lower than the yield point at 1200° F. It has been proved by measurements that after stress relieving a small residual stress always remains, also because of the fact that a uniform cooling down is impossible. This is the reason why in normalized plates a small residual stress also occurs. If a normalized plate is bent cold to a cylinder, or flattened, it is obvious that again a residual stress up to the yield point has to be taken into account. The fact that the high residual

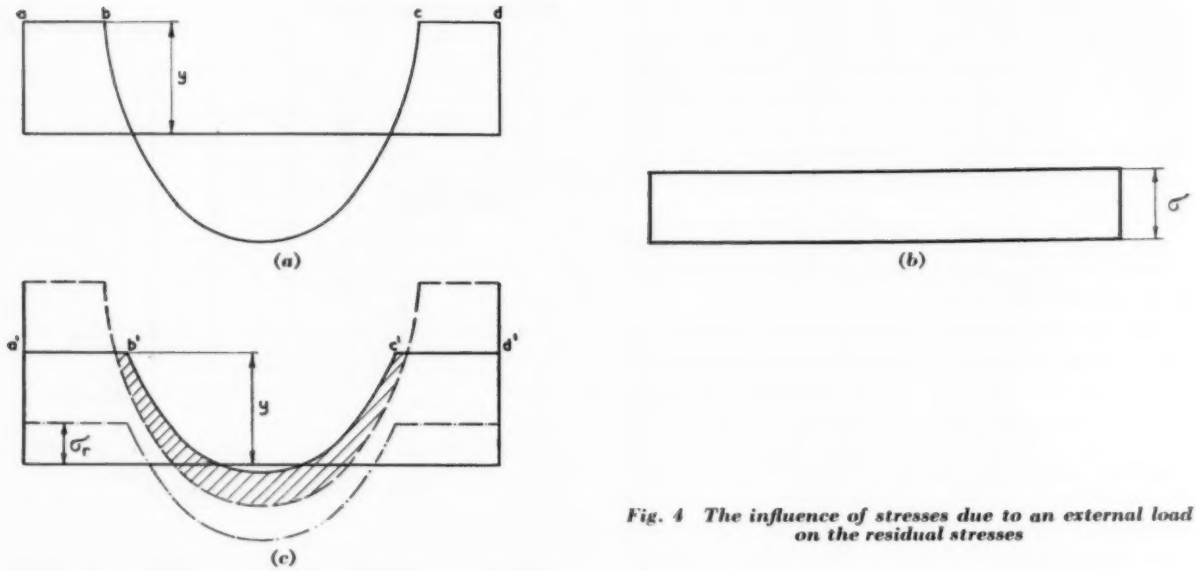


Fig. 4 The influence of stresses due to an external load on the residual stresses

stress also occurs in the nonwelded plate, makes a comparison between riveted and welded vessels possible. Many years of practice have shown that stress relieving of riveted boilers is not necessary. As the above considerations show, it is very doubtful whether stress relieving has to be carried out for welded vessels.

The sum of all the residual stresses distributed over a cross section of the plate or structure must always be zero. This implies that tensile stresses have always to be kept in equilibrium by compressive stresses at other points. This is very important, as will be proved in the following paragraph.

6. THE INFLUENCE OF EXTERNAL LOAD ON RESIDUAL STRESSES

To simplify the following analysis it is assumed that for relatively thin plates the residual stresses vary only in the plane of the plate, but remain constant over the plate thickness. This is of course not quite true, but it does not alter the total result of the following considerations very much.

Figure 4(*a*) shows an arbitrary curve of residual stresses along a weld or other section of the base material. In accordance with the law of equilibrium the area above the zero line must be equal to the one below the zero line. On the planes *ab* and *cd* the yield point Y has been reached.

Suppose this plate is now loaded by a uniform stress "σ" smaller than the yield point of the material, shown in Fig. 4(*b*). This stress has to be superimposed on the original residual stress, giving a total stress as shown in Fig. 4(*c*) with a dotted line. It is, however, impossible that the stresses along the lines *ab* and *cd* are above the yield point and the only possibility is that the new stress follows the solid line $a'b'c'd'$. In accordance with the law of equilibrium again the area above the lines $a'b'$ and $c'd'$ must be equal to the hatched area below. It is true that further yielding occurs locally along the lines $a'b'$ and $c'd'$. Between b' and c', however, the stresses are fully elastic. This is the reason why the local yielding causes a deformation only slightly larger than that in the elastic state of stress. The accepted simplification that the deformation above the yield point is always small so that strain hardening can be neglected is herewith justified.

During the relief of the external load the material behaves completely elastically, this having been proved by many tests. After relieving, the stress shown with a dot-dash line remains. It is obvious that the original maximum residual stress (equal to the yield point) has decreased to an ultimate value $\sigma_r = Y - \sigma$, wherein Y is the yield point and σ the stress due to the external load. Consider now a single spot of the plate or the weld where the original residual stress is equal to the yield point of the material. The plate is now loaded with a uniformly distributed membrane stress and a superimposed stress concentration of the bending type as described in Section 4 (*a*). This total stress is a function of the membrane stress σ_m and can be written as $C\sigma_m$, wherein C is the stress concentration factor. The stress distribution over the plate thickness, supposing that Hooke's law is followed, is shown in Fig. 5(*a*). On the left the stress is $C\sigma_m$, in the heart of the plate σ_m, while the stress on the right is $(2 - C)\sigma_m$. These stresses have to be superimposed on the residual stresses. As locally the plate is loaded to the yield point by these residual stresses, no further increase of stresses is possible at these spots. If now the internal pressure is relieved, the stresses shown on Fig. 5(*a*) have to be subtracted from the existing stress, being the yield point. The remaining residual stress is $Y - C\sigma_m$ at the left side of the plate, $Y - \sigma_m$ in the heart of the plate and $Y - (2 - C)\sigma_m$ at the right side of the plate. See Fig. 5(*b*).

These ultimate residual stresses have to be taken into account.

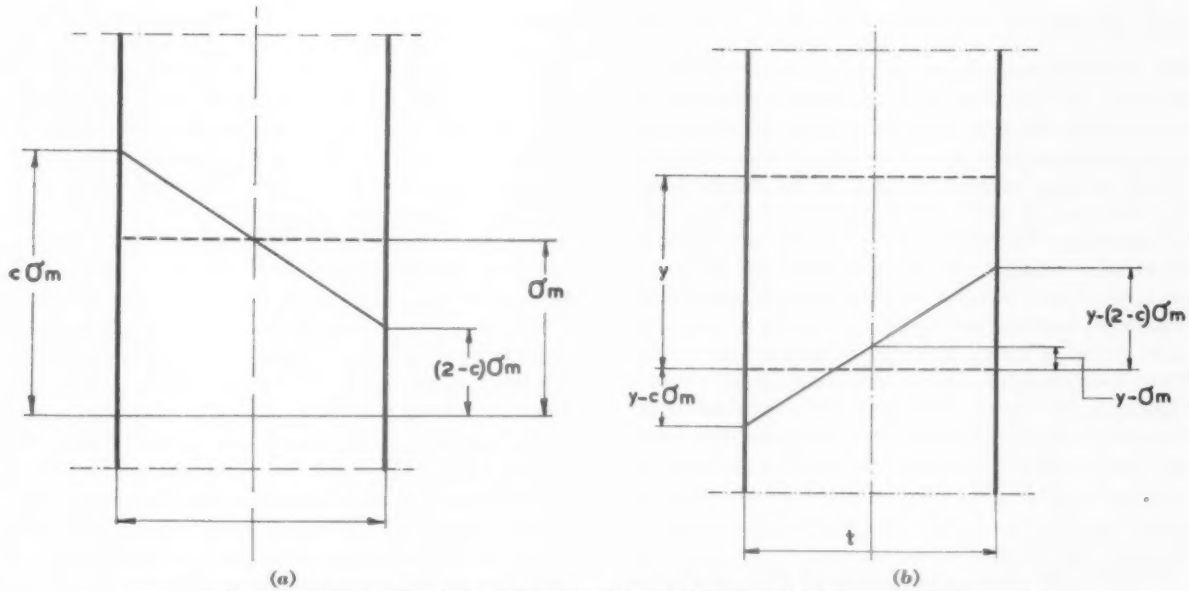


Fig. 5 Theoretical stress distribution over the plate thickness and ultimate residual stresses

It does not matter at all whether the maximum stress $C\sigma_m$ is above the yield point or not. During stress relieving the material behaves elastically as long as $Y - C\sigma_m \geqq -Y$. In this case the ultimate residual stress never reaches the yield point in compression. Should the yield point in compression have been reached, the stress during loading and unloading would vary from the positive to the negative yield point, which is known to provoke a rupture after few cycles.

From the above equation it follows that $C \leqq 2Y/\sigma_m$. It will be shown on the following pages that the factor C must be smaller for other purposes. The yield point in compression is thus never reached; the total strain of the material is limited in such a way that no rupture is to be expected.

7. THE INFLUENCE OF THE HYDROSTATIC TEST PRESSURE FOLLOWED BY RELOADING AGAIN TO THE WORKING PRESSURE AT ATMOSPHERIC TEMPERATURE

The membrane stress due to the hydrostatic test pressure must be below the yield point. Otherwise unpermissible deformation occurs during the test. According to the previous considerations the largest ultimate residual stress after hydrostatic testing is $\sigma_r = Y - (2 - C)\ \sigma_t$, wherein σ_t is the membrane stress during the test. When loading the vessel to the membrane working stress σ_m, the total maximum stress is (see Fig. 6 on the right):

$$\sigma_{tot} = \sigma_r + (2 - C)\ \sigma_m$$

or

$$\sigma_{tot} = Y - (\sigma_t - \sigma_m)\ (2 - C)$$

The total stress is smaller than the yield point if $\sigma_t > \sigma_m$ and $2 > C$. It follows, therefore, that the test pressure should always be higher than the working pressure, while on the other hand the stress concentration factor should be smaller than 2.

The fact that the factor C should not be greater than 2 can be proved as follows.

If C is greater than 2 the vessel will yield again locally at a pressure smaller than the working pressure. As soon as the yield point is reached the bending moment cannot further increase. During further increase of the pressure the following occurs.

It was already mentioned that the bending stress varies to a great extent along the wall surface. The yield point is thus only reached very locally, other spots

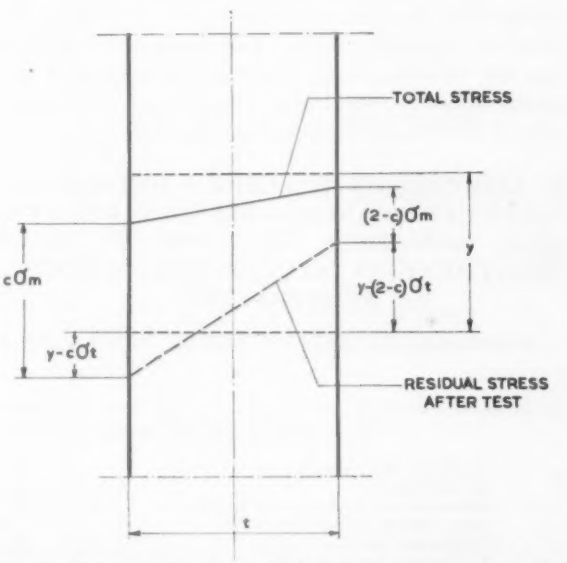


Fig. 6 Total stresses if residual stress is equal to the yield point

in the surroundings remaining fully elastic. As soon as the yield point is reached a small deformation occurs. This deformation, however, must also be very local and therefore can never be large, as large deformation is prevented by the fully elastic surroundings. During a further increase of pressure the greater part of the resultant bending moment is taken up by the surroundings, only a very small part being relaxed by the local deformation. The extra stress in the surroundings therefore increases more than proportionally to the internal pressure. During the relief of the pressure there is a certain instant when the total bending stresses can again be taken up by the formerly highly loaded spot. When loading again takes place the factor C is very slightly smaller than originally, due to the small deformation. It is, however, quite possible that every time the vessel is reloaded in service, a small local deformation occurs until the sum of all deformations together has become so large that the surroundings need no longer take any extra stress.

Fatigue test pieces subject to such high fluctuating loads have shown that, due to the fact that every time the maximum stress occurs, a small deformation is caused; rupture occurs after a very short number of stress variations. To be thus absolutely sure that the local deformation occurs only once during the hydrostatic test, the stress concentration factor should be limited to the value "2." In this case it is absolutely certain that no rupture will occur as a result of too high a strain during the starting-up periods.

A small local strain resulting in a small relaxation of the stress concentration factor also occurs when the vessel is loaded for the first time to the hydrostatic test pressure. Due to this strain the new stress concentration factor, when loading again to the working pressure, is always slightly smaller than used in the calculation. The total stress thus remains always below the yield point if C is limited to "2".

The fact that the above formula gives a somewhat higher stress than that which actually occurs does not influence the results shown below, because of the fact that the equation is only used for comparison of different calculation methods. In this case all the calculated stresses are slightly on the high side.

8. COMPARISON OF VARIOUS METHODS OF CALCULATION AND CONCLUSIONS FOR VESSELS NONSTRESS RELIEVED BY HEAT TREATMENT AT ATMOSPHERIC WORKING TEMPERATURE

According to the American and the British codes a minimum safety factor of 4 to the tensile strength is permitted. The allowable stress for carbon steel is then about 0.45 Y. In the U. S. a weld efficiency of 90% is required for a radiographed nonstress-relieved weld and 80% for a nonradiographed, nonstress-relieved one. In Great Britain a nonradiographed, nonstress-relieved weld for a Class III vessel has a weld efficiency of maximum about 60%. The test pressure for all these vessels is 1.5 times the working pressure. All these requirements are now compared with a nonstress-relieved vessel with an allowable stress of 0.66Y, a test pressure of 1.3 times the working pressure and a weld factor of 100%. The stress concentration factor has been taken as 1.2, 1.4 and 1.6 respectively. The results are shown in Table 1.

The table shows that the highest stress occurs in the British Class III vessel; the American vessel with a weld efficiency of 80% follows. The difference in total stress between the American vessel with a weld efficiency of 90% and the vessel with a weld efficiency of 100% but with a 1.63 times higher allowable membrane stress is negligible. Compared with the Class III vessel a 2.44 times higher membrane stress is permissible without increasing the true total stresses occurring.

It is known that the first three vessels mentioned in the table have proved to be completely safe. According to the same table there is no reason to believe that the last vessel is less safe. The high allowable membrane stress has been adopted in the Swedish, Italian and German codes on the basis of 15 years of experience. A weld efficiency of 100% is to be adopted in the new Netherlands Code, with the same allowable stresses.

From these considerations the following conclusions can be drawn:

(*a*) An allowable membrane stress of two-thirds of the yield point is permissible.

(*b*) A weld efficiency of 100% is permissible.

(*c*) The use of other weld efficiencies lower than 100% is pointless; the total stress is almost completely independent of the magnitude of the weld efficiency.

Further equally important conclusions are:

(*d*) As all the vessels shown in Table 1 have worked safely for many years, all loaded to about the same stress, the high calculated total stress can never be the criterion for rupture. This stress must be safe. Rupture, therefore, can only occur as a result of stress concentrations mentioned in Section 4 (*b*). These stress concentrations have not been taken into account up to now.

Table 1—Total Stress in Various Nonstress-Relieved Vessels at Room Temperature

Tabulated allowable membrane stress	0.450Y	0.450Y	0.450Y	0.660Y
Weld efficiency	90	80	60	100
Allowable membrane stress in weld	0.405Y	0.360Y	0.270Y	0.660Y
Stress during hydrostatic test	0.608Y	0.540Y	0.405Y	0.858Y
Total stress for C = 1.2	0.838Y	0.856Y	0.892Y	0.842Y
Total stress for C = 1.4	0.878Y	0.892Y	0.919Y	0.882Y
Total stress for C = 1.6	0.919Y	0.928Y	0.946Y	0.921Y

(*e*) In Section 7 it was proved that a stress concentration factor of 2 should do no harm during the starting-up periods. Comparison with existing practice shows that this value cannot be dangerous during service, either. Flat end plates, stayed flat walls, flanges, etc., are subject to pure bending. At some spots in such plates the original residual stress will also reach the yield point. If for these parts similar considerations are carried out as done for parts loaded by a membrane stress with superimposed bending stress, it will be found that no stress relaxation as a result of hydrostatic testing is possible, as will be shown in Section 11. This entails that these spots are always loaded to the yield point. As these flat plates, however, do not rupture during service it follows that stresses up to the yield point are permissible, i. e., that C may be equal to 2. From these considerations the conclusion can be drawn that the permissible stress concentrations are proportional and at most equal to the permissible membrane stresses, that is to say that C is limited to the maximum value "2" independent of the magnitude of the allowable membrane stress.

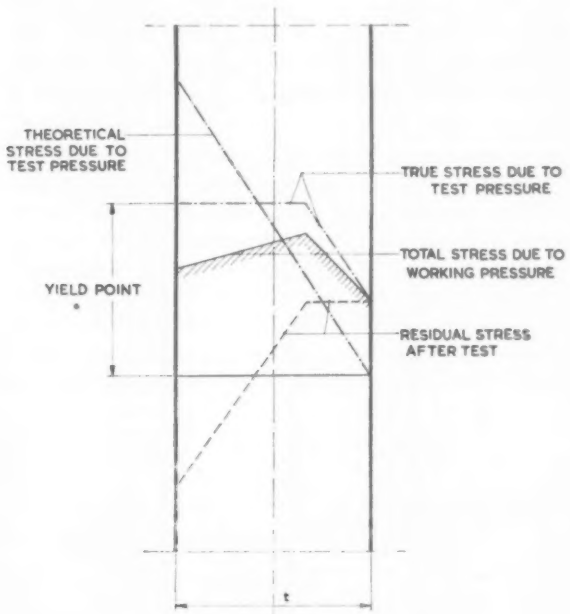

THEORETICAL STRESS DUE TO TEST PRESSURE
TRUE STRESS DUE TO TEST PRESSURE
TOTAL STRESS DUE TO WORKING PRESSURE
YIELD POINT
RESIDUAL STRESS AFTER TEST
t

Fig. 7 Total stress if residual stress is zero

All the above conclusions are based on the fact that at some points in the plate or the weld the residual stress is equal to the yield point in tension. There are also other places where the residual stress is negligible or equal to the yield point in compression. Figures 7 and 8 show the total stresses in both cases, for a membrane stress of $0.66Y$, $C = 2$ and a test pressure of 1.3 times the working pressure. Here, too, the stresses never exceed the yield point, either in tension or in compression.

9. THE CALCULATION OF DOMED ENDS

The stress concentrations so far under consideration occur also in domed ends, conical ends, etc. In some codes only the membrane stresses are taken into account, the bending stresses being neglected or kept below certain values by limiting the knuckle radius. Other codes take the total stress into account, but decrease the safety factor to the tensile strength from four to three; others decrease the safety factor to the yield point from 1.5 to 1.1. Previous considerations have shown that all these methods are more or less arbitrary.

In accordance with the above considerations it is possible to adopt the recommended high membrane stress, 66% of the yield point, and to limit the superimposed bending stresses to the same value. Following this method the minimum height of the dome or the minimum knuckle radius can be calculated.

10. THE INFLUENCE OF OUT-OF-ROUNDNESS, MISALIGNMENT, ETC.

In nearly all codes the permissible out-of-roundness is a function of the diameter of the vessel. The stresses due to out-of-roundness, however, among others are a function of a/t, wherein a is the out-of-roundness and t the plate thickness. Following the tolerances for out-of-roundness given in the codes mentioned it is possible that for very thin-walled vessels the value a/t is large, resulting in an intolerably high stress due to out-of-roundness.

The high allowable stresses mentioned according to the new Netherlands code, combined with a weld

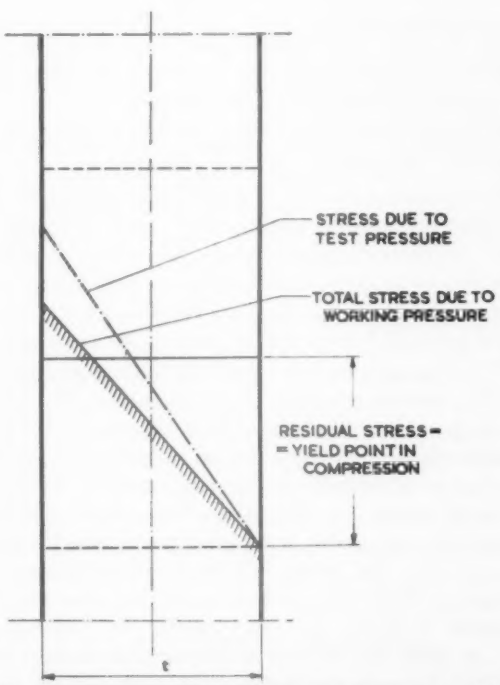


Fig. 8 Total stress if residual stress is equal to yield point in compression

efficiency of 100%, may only be adhered to provided the stresses due to out-of-roundness are kept within certain limits. The procedure followed in this code can be described as follows.

The total theoretically allowable stress is 90% of the yield point. This stress, however, has to be reduced, for nonstress relieving. The total stress now permitted in the vessel, being the sum of the membrane stress and the stress due to out-of-roundness, shall not exceed the above-mentioned reduced stress. This method is illustrated in the following example. To make a comparison possible with the stresses shown in Table 1, the original Netherlands data are modified slightly. This, however, does not alter the principle.

For the example it is assumed that the allowable reduced stress, being 90% of the yield point minus $0.108Y$ for nonstress relieving, is $0.792Y$. This stress is thus equal to $C\sigma_m$. The permissible membrane stress is now $0.792Y/C$, resulting in $0.660Y$, $0.566Y$ and $0.496Y$ for a stress concentration factor $C = 1.2$, 1.4 and 1.6 respectively. The test pressure is kept at 1.3 times the working pressure. The result is shown in Table 2.

Table 2—Influence of Out-of-Roundness

Stress concentration factor	1.2	1.4	1.6
Allowable stress	$0.660Y$	$0.566Y$	$0.496Y$
Stress during hydrostatic test	$0.858Y$	$0.737Y$	$0.648Y$
Total stress	$0.842Y$	$0.898Y$	$0.940Y$

Compared with the last column of Table 1 it is evident that the total stresses have increased slightly. However, if the stress concentration factor due to out-of-roundness is greater than 1.2 the permissible membrane stresses are smaller and the wall thickness greater, compared with those in Table 1. The total stress, however, remains about the same.

It thus seems that the influence of the out-of-roundness is overestimated in the Netherlands code.

The following method of taking into account of out-of-roundness seems logical. First, the influence of stress concentrations due to the permissible misalignment has to be investigated, either by calculation or by stress measurements; say this stress concentration is found to be C_1, being a factor between zero and one.

The total permissible stress concentration factor is 2. For out-of-roundness there remains now only a factor $2 - C_1$. With the aid of this factor the permissible out-of-roundness can be calculated, it being a function of a/t and of the internal pressure. In nearly all cases, except for very thin-walled vessels, the thus calculated permissible out-of-roundness will be much greater than that permitted for practical reasons. A normal practical value for limiting the out-of-roundness, accepted in nearly all codes, except the Netherlands one, is that the difference between the largest and smallest diameter, measured in one cross section, shall not exceed 1% of the diameter. Only in those cases where the out-of-roundness calculated with a stress concentration factor $2 - C_1$ is smaller than the practical value mentioned is a smaller tolerance on ovalness required.

11. THE CALCULATION OF FLAT-END PLATES

Years ago very thin flat-end plates were designed without any calculation or with the aid of a very simple calculation. In the last few years better calculation methods have been developed, with the result that flat end plates are much thicker than those used before. Nevertheless, the thinner plates have been found good and safe in practice and, assuming the up-to-date calculation to be sound, there must be something wrong with the allowable stresses.

In flat-end plates no membrane stress exists. The stresses occurring are of the pure bending type. For the membrane stress an allowable value of 66% of the yield point is permissible. Supposing 100% of the yield point had been allowed, the vessel wall should always yield over the whole thickness. The allowable stress is thus 66% of that which causes yielding over the whole wall thickness. When pure bending stresses occur the plate yields over the whole thickness if the diagram has the shape shown in Fig. 9. The moment per unit of plate length is now $M_y = Y(t^2/4)$, wherein Y

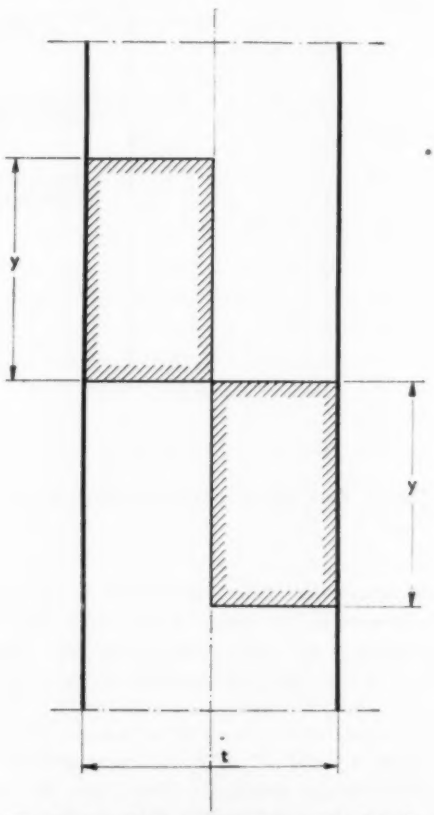


Fig. 9 Bending stresses when the plate is loaded totally above the yield point

is the yield point. If now, just in the same manner as was done with the membrane stress, the allowable bending moment is taken as $M = {}^2/_3 M_y$, the following relation is found

$$M = Y \frac{t^2}{6}$$

The value $t^2/6$ is equal to the section modulus of the plate in the elastic state. From this it follows that the allowable stress in flat-end plates and other plates subject to pure bending is equal to the yield point of the material. Now the same method has been followed for bending stresses and membrane stresses.

It is interesting to investigate what happens when residual stresses exist in the plate. If the original residual stress is equal to the yield point, in compression or in tension, the total stress always remains equal to the yield point. When a plate having local spots where yielding due to original residual stress occurs is loaded by superimposed bending moments, the extra stress due to bending cannot be taken up by these spots, but other points in the surroundings take the load and are stressed more severely as a result. This is true, independent of the magnitude of the bending stress. It is thus understandable that the third-class vessels, subject to very low bending stresses, are also always loaded to the yield point at certain spots of the plate. This is the reason why in Section 8 the conclusion was accepted that stresses up to the yield point are permissible and cannot do any harm. Figure 10 shows the total stress after hydrostatically testing and reloading to the working pressure in the case where no original residual stress exists. Figure 11 shows the same where the original residual stress is equal to 60% of the yield point. In all these cases the total stress remains below the yield point.

The above considerations prove that the recommended stresses are indeed permissible. Lower allowable bending stresses result locally in the same total stress, this being the yield point.

It is obvious that by following this method the thickness of end plates can be reduced, as mentioned already at the beginning of this paragraph.

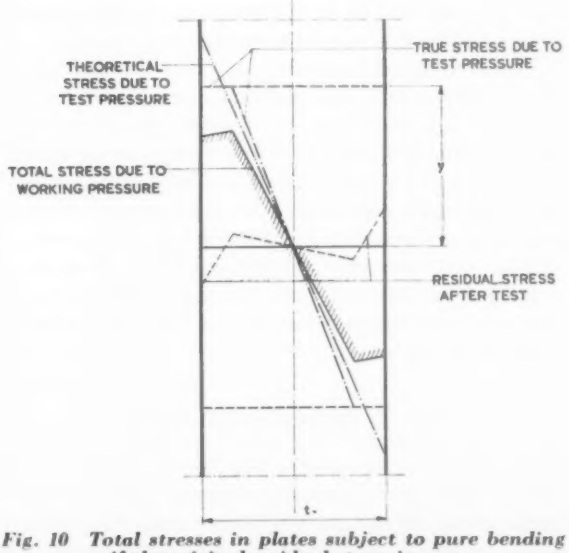

THEORETICAL STRESS DUE TO TEST PRESSURE
TOTAL STRESS DUE TO WORKING PRESSURE
TRUE STRESS DUE TO TEST PRESSURE
RESIDUAL STRESS AFTER TEST
y
t

Fig. 10 Total stresses in plates subject to pure bending if the original residual stress is zero

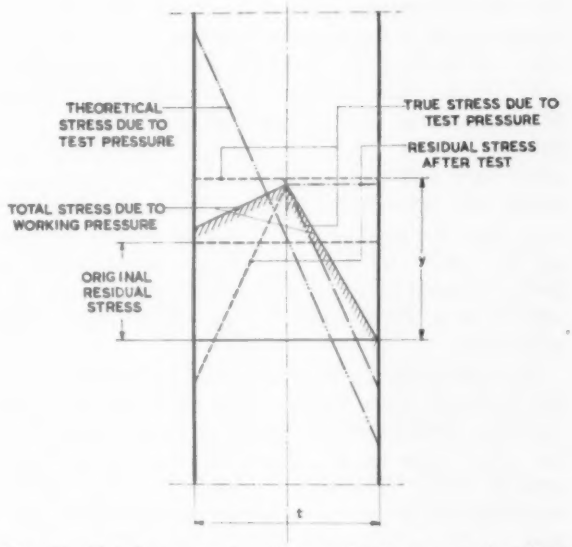


Fig. 11 Total stresses in plates subject to pure bending if the original residual stress is 60% of the yield point

12. TESTING TO RUPTURE

Sometimes a vessel is tested to rupture to check the adopted factor of safety. When the calculation has been carried out with a safety factor of 4 and the vessel ruptures at a pressure of four times the working pressure, everything seems satisfactory.

During testing to rupture, however, at a certain pressure the vessel yields totally. In this case such a tremendous deformation occurs that the stress concentrations mentioned in Section 4 (*a*) relax totally. During total yielding the vessel is only loaded by the membrane stresses calculated.

In service, however, the vessel is locally always loaded to about the yield point, independent of the membrane stress used in the calculation. Thus it is obvious that neither the rupture test nor the acceptance of a factor of safety can be relied upon. Furthermore the rupture during the test is nearly always of the shear type, while always in practice a brittle rupture occurs. On the other hand it is quite possible that although a vessel has passed the rupture test satisfactorily, nevertheless a similar vessel in practice ruptures after some time of service. As the loading to the yield point cannot be the reason for rupture (otherwise all nonstress-relieved vessels would fail after some time) there must be another reason for such occurrences.

13. THEORY OF RUPTURE

(*a*) *At Subatmospheric Temperatures*

To study the behavior of rupture it is, of course,

necessary to examine cases of fracture in practice. At subatmospheric temperatures rupture always has its origin in faults in the weld (cracks, bad penetration, etc.), in the plate (cracks) and sometimes in welded details, such as pipe connections, supports, etc. In Section 4 (*b*) it was mentioned that near cracks and spots with bad penetration a triaxial state of stress occurs, the value of the highest stress being about 2.5 to 3 times the yield point and independent of the load on the vessel (see Fig. 3). In the neighborhood of badly designed or manufactured pipe connections and other details, stress concentrations of the same order may occur.

It is generally accepted that steel has a brittle fracture strength, quite different from the ultimate tensile strength where failure is caused by shear stresses. If the stress value exceeds this brittle fracture strength, rupture starts. This is only possible for relatively low temperatures, below the so-called transition temperature.

Rupture need not start directly after the vessel has been put into service—very often the vessel behaves well in the beginning. After some time, however, age hardening has occurred, which is sometimes just enough to start a brittle rupture. At subatmospheric temperatures only the very high stress concentrations due to faults in the weld or in the plate or due to a bad design are responsible for the rupture.

(*b*) *At Temperatures Above the Transition Temperature*

As at subatmospheric temperatures rupture can always be prevented by selecting an appropriate ductile material, this is not always possible in a higher temperature range. To study the behavior of rupture in the latter case, it will be necessary to examine the experience and to investigate the circumstances under which a fracture has occurred.

First of all it is a fact that rupture can occur in vessels calculated with low theoretical stresses as well as in vessels under high theoretical stresses. The rupture seems to be independent of the membrane stresses occurring.

Secondly, a rupture during service has always a brittle appearance. Riveted vessels sometimes show cracks or complete failure after a long period of service. The rupture always occurs near rivet holes, stay bolts, expanded tubes, etc. Modern stress theories have proved that these spots are subject to very high stress concentrations of the triaxial type, above the yield point, as described in Section 4 (*b*). An attendant circumstance is that mostly, if these failures occur, the fabrication is so bad that the medium which the vessel contains can penetrate between the riveted plates or into gaps near staybolts, etc. It seems, therefore, that the content of the vessel, even if not corrosive in the common sense of the word, exercises a considerable influence.

If a rupture occurs in a welded vessel, the origin is a crack or other fault in the weld, in the plate or some other spot of high triaxial stress concentration as mentioned in Section 4 (*b*). Here the danger of rupture is less than for riveted vessels because of the fact that the medium cannot penetrate into a crack, if not situated at the surface of the plate.

The final important point is that, in the beginning of the life of a boiler or pressure vessel, the material is strong enough to withstand this high triaxial stress. The only possibility is that during the life of the vessel the mechanical properties gradually decrease.

In Section 4 (*b*) it was mentioned that if at certain spots a triaxial state of stress occurs and at the same time the material yields, the magnitude of the stresses is practically only a function of the shape and not dependent on the internal pressure. This is the reason that rupture may also occur in vessels calculated with a low allowable membrane stress. An increase in the allowable stresses, as proposed above by accepting a weld efficiency of 100%, can thus be effected without increasing the danger of rupture.

The other facts mentioned can be explained by considering fatigue to be the cause of rupture. The vessel is always semistatically loaded, that is to say that always small stress variations occur. During loading and reloading, a state which occurs several times during the life of the vessel, even very high stress variations occur.

It is a known fact that the fatigue strength decreases to a very great extent if the state of stress is triaxial. This is not the case with statically loaded specimens subject to a triaxial state of stress, which are mostly stronger than specimens under uni- or diaxial tension.

If fatigue tests are carried out with wetted test bars, the life of the specimen is much shorter compared with that of dry test pieces. Herewith the influence of the medium is confirmed.

A rupture as a result of fatigue has a brittle appearance, whereas a rupture as a result of too high a static load is nearly always a shear fracture.

It seems logical to assume that rupture only occurs due to fatigue at spots where the stresses are triaxial. Probably the other parts, where the state of stress is diaxial, can withstand the stress variations mentioned.

As the rupture is connected with a gradual decrease in mechanical properties under the influence of very high stress concentrations, it is obvious that a test to rupture, to check the safety factor, is of no use at all.

It is even doubtful whether the ultimate tensile strength of the material has any importance, except for checking its quality.

14. STRESS-RELIEVING

Up to now only nonstress-relieved vessels, which always have spots where the residual stress is locally equal to the yield point, have been considered. Due to stress relieving the residual stresses decrease. It

seems clear that the residual stress after heat treatment at 1200° F cannot be much lower than the yield point at that temperature. Data collected in the U. S. A. for carbon steel show that the ratio of the yield point at 1200° F to that at atmospheric temperature varied from 0.16 to 0.34. The residual stress is thus at least $0.16Y$. Due to nonuniform cooling, however, the residual stress can be much higher.

When the sum of the residual stress after stress relieving and the hydrostatic proof membrane stress is equal to, or above, the yield point at room temperature, the total stresses remain the same as in the nonstress-relieved vessel. This is the case with the recommended hydrostatic test pressure, causing stresses of $0.858Y$. In other cases when the allowable stress is, for instance, one quarter of the tensile strength combined with the maximum weld efficiency of 95% or other lower values, the total stresses are lower.

The various calculated stress curves are shown in Fig. 12.

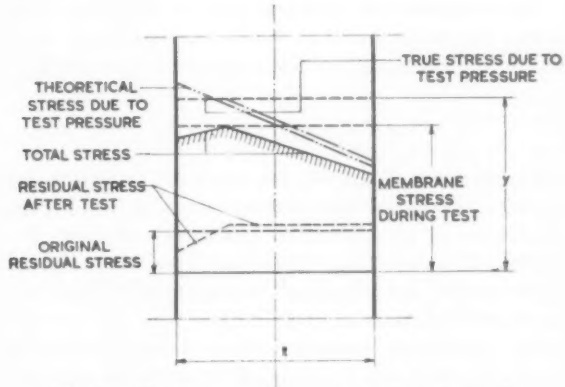


Fig. 12 Total stresses in stress-relieved vessels

From this consideration it follows that if the allowable stress is not too high, a stress-relieved vessel is possibly subject to a lower stress than a nonstress-relieved one, notwithstanding the fact that the weld efficiency of the latter is lower (often much lower).

As, however, the high total stresses occurring in nonstress-relieved vessels are fully permissible, the expensive heat treatment is not necessary from a mechanical point of view.

Stress-relieving will therefore only be carried out if necessary for other purposes, for instance:

(*a*) To improve the mechanical properties, viz., the hardness and notch sensitivity. For this reason many low-alloy steels have to be stress relieved. The notch sensitivity of carbon steel can also be improved by stress relieving and therefore vessels made of carbon steel and subject to low service temperatures should also be stress relieved.

(*b*) In thick-walled vessels a triaxial state of stress occurs. In this case stress relieving such vessels is perhaps necessary. As far as is known no experience exists with nonstress-relieved thick-walled vessels. To be on the safe side, stress relieving should therefore be required.

15. RADIOGRAPHIC EXAMINATION

The only logical conclusion which can be drawn from the theory is that the allowable stress in nonradiographed and radiographed vessels should be the same, the more so as the high stress near cracks (see Fig. 3), which can be avoided by radiographic examination, is not dependent on the magnitude of the membrane stresses occurring.

If a weld is difficult to make without cracks or other faults developing, radiographing is required. For this reason thick vessels should be radiographed. For thin-walled vessels spot radiographing will be perhaps sufficient to examine the welding procedure or the welders.

As cracks may be very dangerous in vessels for low-temperature service, these apparatuses should also be radiographed.

Only in the case where the duty of the vessel is not severe, so that no explosion occurs as a result of rupture, radiographing may be omitted.

16. VESSELS FOR HIGHER SERVICE TEMPERATURE UP TO 650° F

At increasing temperature, the yield point decreases. Therefore, at higher temperatures the allowable membrane stress should be two-thirds of the yield point at the service temperature of the vessel. The test pressure remains 1.3 times such working pressure as the vessel can withstand at atmospheric temperature, causing a stress $0.858Y$, because according to the formula the test pressure should be as high as possible.

According to the American and British codes, the allowable stress remains constant up to 650° F. The total stress relative to the hot yield point, however, increases because of the fact that the yield point decreases. At 650° F the hot yield point is about 68% of the yield point at atmospheric temperature. If the stresses are expressed in terms of the hot yield point, the stresses at atmospheric temperature have to be divided by 0.68. Table 3 gives the new stresses at 650° F, relative to the hot yield point Y_h, for nonstress-relieved vessels.

From Table 3 it follows that the lowest total stress occurs in the vessel with the highest allowable stress.

Nearly all the stresses are theoretically above the yield point. This is, of course, impossible in practice. Up to the temperature at which a real yield point exists, which is about 570° F, the material yields during the heating-up period, so that in reality the total stress is equal to the yield point, independent of the membrane stress allowed. This again is a proof that a stress equal to the yield point cannot be dangerous, because it always occurs.

Above about 570° F no real yield point exists; that is to say, instead of a horizontal part in the stress-strain curve only a kink is shown. In this case the simple theory no longer holds good, as mentioned before.

It is, however, a known fact that above about 500° F a relaxation of all statically indeterminate stresses takes place. The higher the temperature the sooner and the

Table 3—Total Stresses at 650° F in Nonstress-Relieved Vessels

	According to codes			*New proposal*
Tabulated allowable membrane stress	$0.450Y$	$0.450Y$	$0.450Y$	$0.660Yh = 0.450Y$
Weld efficiency	90	80	60	100
Allowable membrane stress in weld	$0.405Y$	$0.360Y$	$0.270Y$	$0.660Yh = 0.450Y$
Stress during hydrostatic test	$0.608Y$	$0.540Y$	$0.405Y$	$0.858Y$
Total stress at 650° F for $C = 1.20$	$1.225Yh$	$1.254Yh$	$1.308Yh$	$0.990Yh$
Total stress at 650° F for $C = 1.40$	$1.285Yh$	$1.308Yh$	$1.346Yh$	$1.150Yh$
Total stress at 650° F for $C = 1.60$	$1.345Yh$	$1.359Yh$	$1.387Yh$	$1.235Yh$

more the residual stresses and the bending stresses will decrease. At 650° F, for instance, probably a great many of the residual stresses and bending stresses have relaxed after some time, so that the total stress is not much higher than the yield point. The fact, thus, that above 570° F no real yield point occurs does not mean that no decrease in stress takes place. Stress measurements have proved that the relaxation mentioned does indeed take place. There is thus a good reason to assume that the stress always follows the yield point curve and at higher temperatures follows approximately the 0.2% strain curve.

The last column of Table 3 shows that if the proposed method of calculation is followed, the total stresses at atmospheric temperature are lower than those calculated with other methods, shown in the first three columns. As a relaxation of the stresses must occur, the final stress at the working temperature is reached with less deformation in the vessel which has been calculated as proposed. This is important because the deformation may occur at a temperature where some materials are hot-brittle. The less the deformation is, the less the possibility of strain aging and rupture.

17. GENERAL REMARKS

The recommended allowable stresses may seem very high to more conservative people. Apart from the fact that an attempt has been made to prove that the total stresses are always about the same, independent of the allowable stress used for the calculation, the proposed allowable stress is totally in agreement with common practice.

An allowable membrane stress of two-thirds of the yield point has already been used in some European codes for many years. Such stress values are also permitted for storage tanks, pipes, steel structures and bridges. In the last few years there has even been a tendency to increase the allowable stresses in steel structures to 86% of the yield point. Compared with those stresses the proposed allowable stresses seem to be low. The high allowable bending stresses to be superimposed on the membrane stresses are not higher than normally occurring in pressure vessels.

The allowable pure bending stresses of 100% of the yield point have already been used by the author and others for many years for the calculation of flat end plates and flanges. Such high stresses have also been allowed for many years in flat tank tops for submarines, etc. Furthermore, as steel structures are calculated according to the theory of plasticity, in a calculation method already used many years before the war with great success in the Netherlands by Professor Kist and others, and in the last few years advocated by Professor Baker in Great Britain, similar bending stresses are permitted.

The only thing that has been done in this report is to try to prove, with a calculation in accordance with the above-mentioned theory of plasticity, that these high stresses are not only permissible, but also that they do not alter the true total stresses occurring.

The recommended weld efficiency of 100% has been in use for many years in steel structures with total success. Here again nothing new has been proposed.

18. CONCLUSIONS

The problem of the stresses occurring in boilers and pressure vessels has been studied with the aid of both the theory of plasticity and the theory of elasticity, taking into account the residual stresses in the weld and in the nonwelded plate and the stress-relieving effect of the hydrostatic test.

The calculated maximum total stress occurring is nearly always equal to the yield point of the material and independent of the allowable membrane stress and weld efficiency used for the calculation.

As such a high stress always occurs, it cannot have any influence on the rupture of the vessel. Rupture can only be due to cracks or other faults in manufacture and/or design. Such faults result in stress concentrations far above the yield point. In this case the state of stress is triaxial. It is known that the material only fails as a result of those high stress concentrations.

As the total stress occurring is not dependent on the membrane stress used for calculation, the latter should be as high as possible.

An allowable membrane stress of 66% of the yield point is recommended. The permissible superimposed bending stress can also be 66% of the yield point. In case pure bending stresses exist, the allowable stress can be equal to the yield point.

The test pressure should load the vessel to a stress 1.3 times the allowable membrane stress at atmospheric temperature.

The proposed allowable stress and test pressure limit the deformation of the material in such a way that no rupture can occur due to excessive strain.

The weld efficiency should always be 100%.

The use of a factor of safety is not based on sound reasoning.

Stress relieving is not necessary with a view to strength and should only be carried out for improvement of the mechanical properties of the material and perhaps in the case of very thick shells.

Radiographing should be carried out when a possible rupture would cause an explosion and in all other cases where a rupture is dangerous. In some cases, if the weldability of the material is low, radiographic examination is also required.

Only good design, excellent manufacture and adequate inspection can prevent rupture in service. The allowable stress is far less important and bad design and manufacture can never be compensated by keeping the allowable stress low.

Literature Survey of High-Strength Steels

Composition and properties of American and European high-yield-strength steels studied by Subcommittee of Pressure Vessel Research Committee

THE majority of pressure vessels operating in the temperature range of −50 to +750° F are fabricated of carbon steel according to ASME Specifications SA-285, SA-201 and SA-212, with or without steel impact tests required. The specified minimum tensile strengths are in the range 45,000 to 70,000 psi. The specified minimum yield points of these steels as-rolled are close to one-half of their tensile strengths. In many applications, the pressures specified for vessels are so high that wall thicknesses of 4 to 6 in., or even more, are required with the use of these materials. Such heavy wall thicknesses impose too high temperature stresses where temperature fluctuations occur and, to a degree, are undesirable because of the extra weights of steel thereby required.

The design of pressure vessels under the two Codes recognized in the U. S. and Canada is at present based on one-fourth of the tensile strength, but practical consideration already is being given to the as-normalized yield strength at (metal) temperature as the basis for design, using one-half, or, possibly five-eights, of that physical property as the design stress value in the employment of high-yield-strength steels. The advantages to be gained thereby are illustrated by comparing the limits on present-day use of a 70,000-psi tensile steel made, for example, to ASME Spec. SA-212, Grade B, with the potential use of a somewhat stronger steel having an as-normalized yield strength of 70,000 psi. The design stress value for the SA-212, Grade B steel is one-fourth of its tensile strength, i.e., 17,500 psi, which is a little less than one-half of its specified minimum yield strength. The use of a 70,000-psi as-normalized yield-strength steel, on the other hand, might be permitted a design stress value of 35,000 psi or more, or at least twice that for the SA-212, Grade B steel, as compared with a design stress of 22,500 psi if design were based on one-fourth of the ultimate tensile strength. High-yield-strength steels have therefore attracted the interest of the designer, fabricator and user of pressure vessels in high-pressure service. The suitability of such steels as materials for pressure vessels is being investigated by the Pressure Vessel Research Committee. It established for this purpose a special Subcommittee on Research on High-Strength Steels, and the first task assigned to this Subcommittee was to prepare a literature survey of high-strength steels, which is reproduced in the accompanying tabulation.

Since steels with yield strengths of up to 45,000 psi have been used in pressure vessels for some time, the stipulation is made that, in order to qualify as high-strength steel in the sense of the foregoing explanations, a steel should possess a minimum yield strength or yield point of 50,000 psi. Since plates as light as $^1/_4$ in. may be used in pressure vessel construction, materials which exhibit this yield-strength level at thicknesses of $^1/_2$ in. or under have been included in the tabulation and, whenever available, the actual thickness range corresponding to the tabulated properties has been listed. The properties listed are, for the most part, those of as-rolled, normalized or normalized and tempered plates since a heat treatment involving quenching into a liquid medium may have certain limitations for heavy-walled pressure vessels. In a few instances, however, quenched and tempered properties are listed, as such plates, supplied in the quenched and tempered condition, might be used if fabrication does not require hot forming and consequently reheat treatment, and if stress relieving of the finished vessel is either not necessary, or can be accomplished without seriously lowering the yield strength.

In making a survey of the technical literature for high-yield-strength steels, weldability had to be con-

Prepared by Subcommittee on Research on High-Strength Steels, Materials Division, PVRC; F. Eberle, *Chairman*, J. Hodge, N. A. Kahn and W. Spraragen.

Trade Name or Designation	Composition													
	C	Mn	Si	P	Cr	Ni	Cu	Mo	V	Zr	Al	Ti	N2	B
Commercial Low-Alloy Plate Steels														
1. Aldecor (2)	≤.12	.15/.40	.35/.75	.08/.15	-	-	.35/.65	.16/.28	-	-	-	-	-	-
2. Double-Strength (2)	≤.12	.15/1.0	-	≤.04	-	.50/1.1	.50/1.0	≥.10	-	-	-	-	-	-
3. MES-70	.10/.20	.50/.90	-	-	-	1.4/1.85	1.0/1.3	.20/.30	-	-	-	-	-	-
4. Cor-Ten	≤.12	.20/.50	.25/.75	.07/.15	.50/1.25	≤.65	.25/.55	-	-	-	-	-	-	-
5. Man-Ten	≤.25	1.1/1.6	≤.30	≤.045	-	-	≥.20	-	-	-	-	-	-	-
6. U.S.S. Tri-Ten	≤.22	≤1.25	.10/.30	≤.045	-	.50/1.0	.30/.50	-	-	-	-	-	-	-
7. U.S.S. Tri-Ten E	≤.22	≤1.25	≤.30	≤.04	-	-	≥.20	-	-	-	-	-	-	-
8. U.S.S. Carilloy T-1(5)	≤.18	.70/1.0	.15/.25	-	-	.70/1.0	.20/.40	.35/.50	.03/.08	-	-	-	-	-
9. Mn-Ni-Cu (2)	≤.25	≤1.40	≤.25	≤.045	-	.50/1.0	.30/.60	-	-	-	-	-	-	-
10. Hi-Steel (2)	≤.12	.50/.90	≤.15	.05/.10	-	.45/.75	.95/1.3	.08/.18	-	-	.12/.27	-	-	-
11. NAX High Tensile (2)	.08/.15	.50/.75	.60/.90	≤.04	.50/.65	-	-	-	-	.05/.15	-	-	-	-
12. N-A-XTRA 75	≤.20	.50/.70	.50/.80	≤.04	.50/.70	-	-	.10/.20	-	.05/.15	-	-	-	-
13. N-A-XTRA 100	≤.20	.50/.70	.50/.80	≤.04	.50/.70	-	-	.10/.20	-	.05/.15	-	-	-	-
14. N-A-XTRA 125	≤.30	.50/.70	.50/.80	≤.04	.50/.70	-	-	.10/.20	-	.05/.15	-	-	-	-
15. N-A-XTRA 150	≤.30	.50/.70	.50/.80	≤.04	.50/.70	-	-	.10/.20	-	.05/.15	-	-	-	-
16. Yoloy HS	≤.15	≤.75	≤.30	≤.10	-	1.5/2.0	.75/1.25	-	-	-	-	-	-	-
17. Yoloy E HS	≤.16	≤.90	≤.30	≤.10	.20/.35	.30/.60	.25/.50	-	-	-	-	-	-	-
18. Jalten #1 (1)	≤.15	≤1.30	≤.10	≤.04	-	-	≤.30	-	.035/.065	-	-	-	-	-
19. Jalten #2 (1)	≤.15	≤1.40	≤.10	≤.14	-	-	≥.30	-	-	-	-	-	-	-
20. Jalten #3 (1)	≤.25	≤1.50	≤.30	≤.04	-	-	≥.20	-	-	-	-	-	-	-
21. Jalloy 1	.13/.18	1.0/1.3	.15/.30	≤.04	-	-	-	.10/.20	-	-	-	-	-	-
22. Jalloy 3	.25/.30	1.3/1.65	.15/.30	≤.04	-	-	-	.10/.15	-	-	-	-	-	-
23. Kaisaloy	≤.18	≤1.50	≤.35	≤.04	≤.20	≤.30	≤.35	≤.10	≥.02	-	-	≥.005	-	-
24. Dynel (3)	.12	.50	-	.08	-	-	.40	-	-	-	-	-	-	-
25. AW70-90 (3)	.25	.50	.25	.10	.25	.25	.50	-	-	-	-	-	-	-
26. HT-50	.10	.60	-	.10	-	.60	.60	.20	-	-	-	-	-	-
27. Stelcoloy	.08/.12	.60/.90	-	.08/.12	-	.50/.70	.70/.80	-	-	-	-	-	-	-
28. Mn-Mo (ASTM A-302 (Gr. B)	≤.25	1 1/1.5	.15/.30	≤.04	-	-	-	.40/.60	-	-	-	-	-	-
Navy High-Tensile Steels														
29. Ht. 471 (6)	.15	1.12	.29	.020	.05	.12	.12	.02	.04	-	.01	.012	-	-
30. Ht. 487 (6)	.14	1.14	.35	.041	.07	14	.12	.03	.034	-	.01	-	-	-
31. Ht. 488 (6)	.14	.54	.22	025	.04	1.54	.91	.02	≥.005	-	.036	≥.005	-	-
32. Ht. 490 (6)	.17	1.27	.30	.028	.09	.10	.17	.02	≤.005	-	.025	.017	-	-
33. Ht. 502 (6)	.17	1.11	.27	.032	.08	.13	.26	.22	≤.005	-	≤.01	.017	-	-
34. Ht. 503 (6)	.14	1.23	.30	.025	.11	.15	.26	.03	≤.005	-	.036	.010	-	-
35. Ht. 504 (6)	.16	1.21	.25	.03	.03	.05	.05	≤.01	≤.005	-	≤.01	.017	-	-
36. Mn-V (7)	.16	1.27	.20	.022	-	-	-	-	.11	-	-	-	-	-
37. Mn-V (8)	≤.18	≤1.45	.15/.30	-	.80/1.1	-	-	-	.08/.14	-	-	-	-	-
38. Mn-V (4)	.16/.18	1.1/1.3	.20/.30	.015/.025	-	-	-	-	.05	-	.01/.02	.01/.02	-	-
39. Mn-Ti (7)	.15	1.30	.24	.02	-	-	-	-	-	-	-	.016	-	-
40. Mn-V-Ti (Navy Spec. 4835) (11)	≤.18	≤1.30	15/.35	-	.15	≤.25	≤.35	≤.06	≥.02	-	-	≥.005	-	-
41. Mn-V-Ti (7)	.15	1.07	.23	.019	-	-	-	-	.055	-	-	.017	-	-
42. Ni-Cu-Mo-V (Navy "Q" Steel)	≤.18	-	-	-	-	2.0/2.5	.55/.85	.30/.40	.08/.13	-	-	-	-	-
High-Phosphorous Steels (4)														
43. a)	.09	.40	.05	.11	-	75	.65	.10	-	-	-	-	-	-
44. b)	.09	.70	.40	.10	.25	.35	.60	-	-	-	-	-	-	-
45. c)	.09	.50	.20	.10	-	.55	1.10	-	-	-	-	-	-	-
46. d)	.09	.40	.45	.09	.90	.50	.40	-	-	-	-	.03/.04	-	-
47. e)	.09	.25	.50	.10	-	-	.50	.20	-	-	-	-	-	-
48. f)	.09	1.10	.05	.12	-	-	.35	-	-	-	-	-	-	-
49. g)	.09	.60	.10	.08	-	.85	.60	-	-	-	-	.06	-	-
50. h)	.09	.60	.15	.10	-	.60	.60	.20	-	-	-	-	-	-

Manufacturer	Yield Point or Yield Str., psi	Tensile Str., psi	Dimensions	Heat Treatment	Endurance Limit, psi	Weldability	Recommended Electrode
1. Republic Steel Corp.	>50,000	>70,000	<½" Plate	Hot-Rolled		Weldable	
2. "	>50,000	>70,000	<½" Plate	" "		"	
3. "	50,000/80,000	70,000/105,000	3/16"-3/4" Plate	Hot-Formed and Stress-Relieved at 1100 F		"	Low-Hydrogen-Coated Electrode
4. U.S. Steel Co. and Republic Steel Corp.	>50,000	>70,000	½" max Plate	As-Rolled	42,000	"	
5. U.S. Steel Co.	>50,000	>70,000	½" max Plate	" "	39,000	"	Mild Steel
6. "	>50,000	>70,000	½" max Plate	" "	42,000	"	
7. "	>50,000	>70,000	½" max Plate	" "	42,000	"	
8. "	>90,000	>105,000	½"-5" Plate	1700 F/Water and 1200 F Temper	60,000	"	AWS E12015
9. "	>50,000	>70,000	½" Plate	Hot-Rolled		"	
10. Inland Steel Co.	>50,000	>70,000	1-2" Plate	" "	49,000	"	
11. Great Lakes Steel Corp.	>50,000	>70,000	½" Plate	" "	45,000	"	
12. "	75,500	95,200	<1" Plate	Quenched and Tempered		"	
13. "	101,000	120,000	<1" Plate	" " "		"	
14. "	125,800	139,000	<1" Plate	" " "		"	
15. "	158,500	165,000	<1" Plate	" " "		"	
16. Youngstown Sheet & Tube Co.	>50,000	>70,000	<½" Plate	As-Rolled	48,000	"	
17. "	>50,000	>70,000	<½" Plate	" "		"	
18. Jones and Laughlin Steel Corp.	>50,000	>55,000	<½" Plate	Hot-Rolled		"	
19. "	>50,000	>70,000	<½" Plate	" "		"	
20. "	>50,000	>70,000	<½" Plate	" "		"	
21. "	50,000	80,000	<1" Plate	" "		"	
22. "	80,000	102,000	<1" Plate	" "		"	E7015, E10015, and E12015
23. Kaiser Steel Corp.	>50,000	>70,000	<1" Plate	" "	44,000	"	E60XX
24. Alan Wood Steel Co.	58,000	72,000	<3/4" Plate	" "	45,000	"	
25. "	70,000	90,000	<3/4" Plate	" "		"	
26. Armco Steel Corp.	>50,000	65-75,000	<½" Plate	" "	43,000	"	
27. Steel Co. of Canada Ltd.	>50,000	>70,000	<½" Plate	" "		"	
28. Lukens Steel Co.	>50,000	80-100,000	2"-4" Plate	" "		"	
29.	Long. 48,750 Trans. 50,150	74,500 74,900	1" Plate			"	
30.	L. 50,500 T. 48,750	73,500 72,900	"			"	
31.	L. 48,900 T. 49,400	72,750 72,400	"			"	
32.	L. 50,000 T. 49,500	76,150 75,550	"			"	
33.	L. 52,500 T. 48,500	73,150 74,750	"			"	
34.	L. 48,150 T. 44,750	72,500 72,750	"			"	
35.	L. 47,400 T. 45,550	68,500 67,750	"			"	
36.	65,100	89,150					
37.	64,500	89,550	½" Plate	As-Rolled			
38.	50-53,000	75-80,000	1" Plate	" "			
39.	52,800	75,800					
40. Bethlehem Steel Co.	>50,000	<92,000	<½" Plate	As-Rolled		Readily Weldable	AWS E70XX
41.	58,400	77,275					
42.	70,000			Normalized			
43.	50-55,000		½" to 3/4" Plate	As-Rolled			
44.	"		"	" "			
45.	"		"	" "			
46.	"		"	" "			
47.	"		"	" "			
48.	"		"	" "			
49.	"		"	" "			
50.	"		"	" "			

Trade Name or Designation	Composition													
	C	Mn	Si	P	Cr	Ni	Cu	Mo	V	Zr	Al	Ti	Nb	B
Low-Phosphorous Steels (4)														
51. a)	.15	.90/1.2	.20	-	-	-	-	.15/.50	-	-	-	-	-	-
52. b)	.15	.90/1.2	.20	-	-	-	-	.15/.50	.10	-	-	-	-	-
53. c)	.15	.50	.85	-	.60	.20	-	-	-	.10	-	-	-	-
54. d)	.15	.75	.85	-	-	-	-	.15	-	.15	-	-	-	-
55. e)	.15	.75	.20	-	-	.75	1.20	.10	-	-	-	-	-	-
56. f)	.15	.50	.30	-	-	1.60	.80	-	-	-	-	-	-	-
57. g)	.15	1.10	.70	-	.50	-	-	-	-	-	-	-	-	-
58. h)	.15	.75	.50	-	.25	.25	.50	.50	-	-	-	-	-	-
59. Mn-Cu Precip.-Hard. Steel (4)	.25	1.25	-	-	-	-	1.25	-	-	-	-	-	-	-
60. Mn-Mo-Ni-V (8)	<.18	<1.30	.15/.30	-	-	1.25/1.75	-	.15/.25	.08/.13	-	-	-	-	-
61. Cr-V (8)	.17/.25	.47/.75	-	-	.15/.30	.15/.30	-	-	.02/.05	-	-	-	-	-
62. Cr-Ni-V (8) (U.S. Army Spec. 57-114-1A, Class B)	.15/.25	.50/.80	.15/.30	-	.15/.30	.15/.30	-	-	.02/.05	-	-	-	-	-
British Steels														
63. Ducol W21 (9)	<.23	<1.70	-	-	<.25	<.50	-	-	-	-	-	-	-	-
64. Ducol W25 (9)	<.20	<1.50	-	-	<.30	<.50	-	<.30	-	-	-	-	-	-
65. Ducol W30 (9)	<.18	<1.40	-	-	<.80	<.50	<.50	<.25	<.10	-	-	-	-	-
66. British Steel (4)	.15	1.10	.20	-	-	.50	-	.25	-	-	-	-	-	-
67. " " (4)	.15	.35	.95	-	.80	-	-	-	-	-	-	-	-	-
68. " " (10)	.16	1.09	-	-	.07	.45	-	.26	-	-	-	-	-	-
69. D-Steel (3)	.33	1.25	.12	-	-	-	-	-	-	-	-	-	-	-
70. Fortiweld (Mo-B) (11)	.13	.63	.29	.029	-	-	-	.36	-	-	-	-	-	.001
71. Ni-Mo (11)	.14	.55	.27	-	-	2.06	-	.30	-	-	-	-	-	-
72. Ni-Mo-B (11)	.15	.57	.30	-	-	2.06	-	.21	-	-	-	-	-	.0026
73. Mn-Mo (12)	.18	1.35	-	-	-	-	-	.25	-	-	-	-	-	-
74. " (13)	<.20	<1.50	<.30	<.06	<.25	<.50	-	<.15	-	-	-	-	-	-
75. " (13)	<.18	<1.40	<.30	<.06	<.25	<.50	-	<.30	-	-	-	-	-	-
76. Chromador (14)	.22	.80	.10	-	.90	-	.30	-	-	-	-	-	-	-
77. Mn-Cr-Ni-Cu (14)	<.30	.90	.30	-	.60	.50	.45	-	-	-	-	-	-	-
78. BS.548 (10)	.25	1.48	.05	-	-	-	-	-	-	-	-	-	-	-
79. UI.1 (10)	.14/.18	1.0/1.4	.30	-	<.25	<.50	-	.20/.30	-	-	-	-	-	-
German Steels														
80. ST52 (3)	.15	1.00	.50	-	-	-	.25	.10	-	-	-	-	-	-
81. ST52 (3)	.18	1.50	.50	-	-	-	.50	-	-	-	-	-	-	-
82. ST52 Commercial (15)	.21	1.33	.47	.033	-	-	.45	-	-	-	.03	-	.006	-
83. ST52 Non-Aging (15)	.18	1.20	.50	.036	-	-	.45	.10	-	-	.08	-	.007	-
84. Cu-Ni 47 Spezial (9)	.20	.70	.40	.04	-	.60	.90	-	-	-	-	-	-	-
85. Cu-Ni 52 Spezial (9)	.20	.90	.40	.04	-	.60	.90	-	-	-	-	-	-	-
86. ST52 DSB (14)	<.20	<1.50	<.50	<.05	-	-	-	-	-	-	-	-	-	-
87. ST52 Cr-Cu (14)	<.20	.80	.30	<.06	.40	-	.40	-	-	-	-	-	-	-
88. ST52 Mo (14)	<.20	1.00	.40	<.06	-	-	-	.20	-	-	-	-	-	-
89. HSB50 (14)	<.20	.95	.45	<.05	-	-	-	-	-	-	-	-	-	-
90. HSB55 (14)	<.20	.80	.40	<.05	-	.70	.90	.15	-	-	-	-	-	-
Austrian Steels														
91. ST 52 T (14)	<.20	1.30	<.60	<.06	-	-	-	-	-	-	-	-	-	-
92. Aldur 58 (14)	.20	1.40	.45	<.04	-	-	.35	-	-	-	-	-	-	-
93. Elstruk (14)	.20	1.60	.45	<.04	-	-	-	.20	-	-	-	-	-	-
French Steels														
94. (4)	.15	.35	.95	-	.80	-	.40	-	-	-	-	-	-	-
95. (4)	.15	.70	.15	-	.50	-	.60	-	-	-	-	-	-	-
96. Ac 50 (14)	.10	.50	.10	-	.40	-	.50	-	-	-	-	-	-	-
97. Ac 54 (14)	.20	.60	.30	-	.45	-	.45	-	-	-	-	-	-	-
Italian Steels														
98. (16)	.14/.25	.80/1.5	.50/1.5	-	.50/1.5	.80/1.8	<.80	<.30	<.25	-	-	-	-	-
99. (16)	.14/.17	1.0/1.2	.80/1.0	-	.80/1.0	1.0/1.2	.30/.50	.15/.20	.10/.15	-	-	-	-	-
100. (16)	.17/.21	1.1/1.3	1.1/1.3	-	.90/1.1	1.1/1.3	-	-	-	-	-	-	-	-
101. (16)	.16/.22	1.1/1.3	1.0/1.3	-	.90/1.1	1.2/1.4	-	.20/.30	.10/.20	-	-	-	-	-
102. (16)	.19/.25	1.1/1.4	1.0/1.3	-	.90/1.2	1.2/1.6	-	-	-	-	-	-	-	-
103. (16)	.14/.17	1.0/1.2	.80/1.0	-	.80/1.0	1.0/1.2	-	-	-	-	-	-	-	-
104. (16)	.14/.17	.80/1.0	.50/.80	-	.80/1.0	.80/1.0	.40/.70	-	-	-	-	-	-	-

Strength Steels (*Continued*)

	Manufacturer	Yield Point or Yield Str., psi	Tensile Str., psi	Dimensions	Heat Treatment	Endurance Limit, psi	Weldability	Recommended Electrode
51.		50-55,000		½" to 3/4" Plate	Not Stated			
52.		"		"	" "			
53.		"		"	" "			
54.		"		"	" "			
55.		"		"	" "			
56.		"		"	" "			
57.		"		"	" "			
58.		"		"	" "			
59.		45,000 65,000	80,000 100,000		Normalized Precip. Hardened			
60.		75,000	>95,000	1" Plate	Norm. and Stress-Rel.			
61.		80,350	91,250	3" Plate	Hot-Rolled			
62.		40-50,000	70-100,000	½" Plate	Hot-Rolled, Stress-Rel.			
63.	Colville	51,520(.5% El.)	>82,880	< ½" Plate				
64.	"	>55,000(.5% El.)	>78,400	3/4"-1½" Plate	As-Rolled and Tempered			
65.	"	>60,000(.5% El.)	>80,000	1"-3" Plate	Normalized and Tempered			
66.		50-55,000	-	½"-3/4" Plate	Not Given			
67.		50-55,000	-	½"-3/4" Plate	Not Given			
68.		64,000	88,000		Not Given			
69.		(No Data Given)						
70.	Appleby-Frodingham	63,392(.1% El.)	100,352	3½" Plate	As-Rolled			
71.		66,976	86,912	-	1616 F/Air			
72.		88,032	102,144	-	1616 F/Air			
73.		45,920(.02% El.)			Hot-Rolled and Tempered			
74.		>53,760	78,400-91,840	<1" Plate	As-Rolled			
75.		60,480	78,400-91,840	<3" Plate	1688F/Air and 932F/Air			
76.		51,204	82,495-96,719					
77.		55,471	85,340					
78.		54,880	92,300	<1½" Plate	As-Rolled			
79.		56,000	78,400-91,800					
80.	Gutehoffnungs Hütte Krupp	50,000	74,000	½"-3/4" Plate				
81.		55,000	85,000					
82.		Long. 51,100 Trans. 49,300	87,000 84,000					
83.		Long. 50,000 Trans. 50,100	82,000 82,000					
84.		48,359	66,850-79,550	<2.3" Plate				
85.		51,200	73,950-88,185	<2.3" Plate				
86.		49,782	73,961-88,185					
87.		49,782	73,961-88,185					
88.		49,782	73,961-88,185					
89.		51,204	71,117-85,340					
90.		51,204	82,495-96,719					
91.		>46,337	73,961-91,029					
92.		56,893	82,495-96,719					
93.		56,893	85,340-99,563					
94.		50-55,000		½"-3/4" Plate				
95.		50-55,000		½"-3/4" Plate				
96.		42,670	71,117-81,073					
97.		51,204	76,806-91,029					
98.			128-199,126		Hot-Rolled or Normalized			
99.			128-156,000		Normalized			
100.			-		-			
101.			156-199,000		Normalized			
102.			-		-			
103.			113-142,000		Normalized			
104.			85-113,000		Normalized			

sidered. In the case of alloy steels, if no information on weldability was given, only compositions having a carbon content of less than 0.30% were listed, although such compositions are, of course, not necessarily weldable. However, it is justifiable to assume that in most cases weldability could be achieved with such alloys by suitable adjustment of the carbon, manganese and alloy contents.

In the tabulation* of properties, information on fatigue has also been included, since fatigue behavior is considered by many to be a significant characteristic of pressure vessels. In all other respects, the given data are self-explanatory.

It should be emphasized, finally, that inclusion in this list does not necessarily imply that the steel is suitable for pressure vessel use, as many of the steels, particularly the commercial high-strength low-alloy steels, would not customarily be sold in a quality suitable for pressure vessel construction. The high-strength low-alloy steels, for example, are customarily sold as structural quality steels to meet minimum mechanical property requirements in the as-rolled condition; while, to be suitable for pressure vessel construction, these steels would have to meet flange and firebox quality requirements.

* No distinction is made in this tabulation between minimum values and ranges which can be employed for specification purposes, and typical values secured on single heats. For this reason, relative comparisons among the steels listed are in many instances unwarranted and can be misleading.

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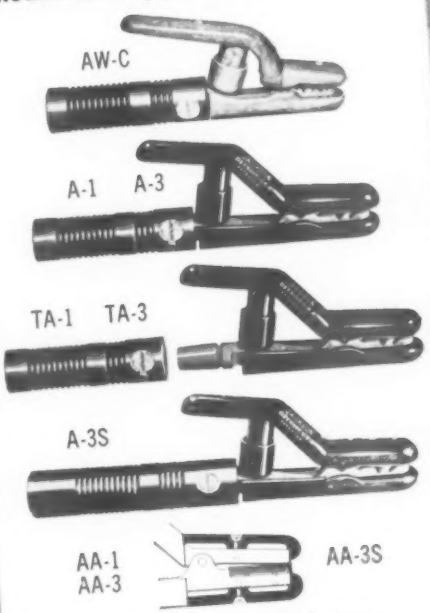

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MEDIUM	A-1, AA-1 TA-1	1/4″	300	1/0	50%
LARGE	A-3, AA-3 TA-3	3/8″	500	3/0	50%
HEAVY-DUTY	A-3S	3/8″	500	3/0	65%
	AA-3S	3/8″	500	4/0	80%

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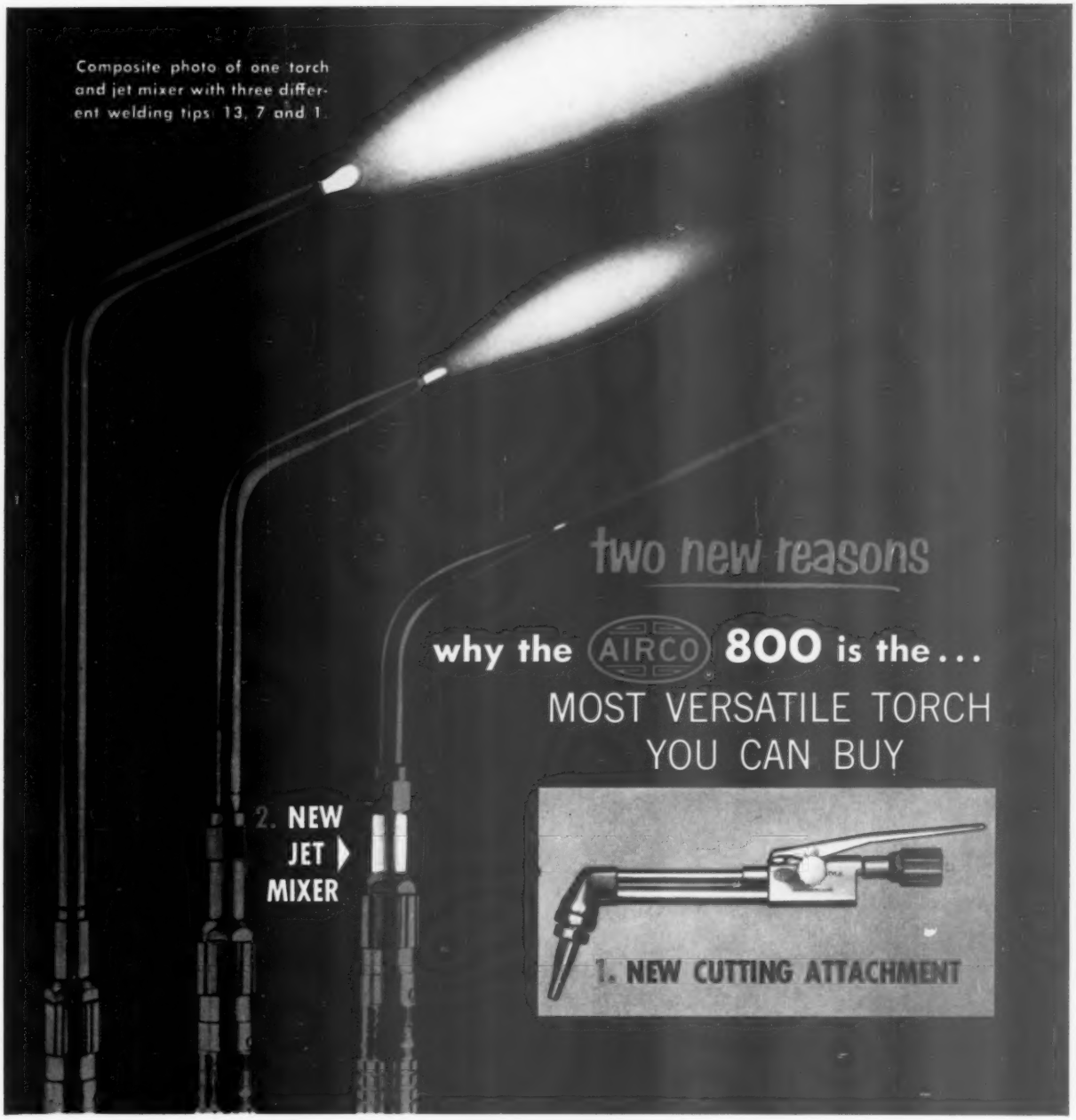
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